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Predefined-Time Direct Lift/Side-Force Control for Carrier Landing

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Highlights

What are the main findings?

- The proposed direct lift/side-force architecture improves vertical and lateral tracking while keeping angle of attack, bank angle, and sideslip close to their landing trim values.
- The predefined-time observer and flap anti-saturation mechanism improve disturbance rejection and retain actuator-feasible landing performance under deck motion, airwake, gusts, and randomized initial errors.

What are the implications of the main findings?

- Direct-force effectors can correct glide-slope and lineup errors with less reliance on attitude-mediated motion, supporting safer touchdown geometry and more consistent arresting-hook engagement.
- Integrating prescribed transient timing with saturation-aware compensation offers a practical approach to robust carrier-landing control when rapid disturbance rejection is required within limited control authority.

Abstract

Carrier-based fixed-wing UAV landing is challenged by deck motion, carrier airwake, gust disturbances, strong trajectory–attitude coupling, and actuator constraints. To address these issues, this paper proposes a Predefined-Time Direct Lift/Side-Force-Integrated Approach Landing (PTDIAL) method. An integrated direct-force architecture is constructed using the trailing-edge flap for direct lift and the spoiler for direct side force, thereby reducing the dependence of trajectory correction on angle-of-attack- and bank-angle/sideslip-mediated regulation. A preview-based reference glide slope is generated from the predicted Ideal Touch Point (ITP) sequence to improve the response to deck motion. Predefined-time control laws are developed for the cascaded position, trajectory, attitude, angular rate, and velocity loops, with prescribed-performance constraints imposed on the attitude response. A predefined-time disturbance observer is introduced to estimate the lumped aerodynamic disturbances, while an auxiliary anti-saturation mechanism compensates for the effect of trailing-edge flap saturation. Lyapunov analysis establishes the practical predefined-time stability of the closed-loop system under bounded disturbances and actuator constraints. Various simulations demonstrate that the proposed architecture improves lateral and vertical tracking while preserving the UAV attitude, and Monte Carlo simulations further confirm the robustness of PTDIAL.



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1. Introduction

Carrier-based fixed-wing unmanned aerial vehicles (UAVs) have attracted increasing attention because of their potential applications in maritime surveillance, reconnaissance, and autonomous strike missions [1]. For such UAVs, automatic carrier landing is one of the most demanding flight tasks, since the vehicle must approach a limited landing area with high accuracy within a short terminal phase [2,3]. During final approach, small deviations in the lateral or vertical position may lead to large touchdown errors near the Ideal Touch Point (ITP) [4]. Therefore, an effective landing controller should ensure fast error convergence, accurate attitude regulation, and reliable disturbance rejection.

The carrier-landing environment is highly challenging due to the combined effects of carrier airwake, deck motion, and atmospheric gusts [5,6]. The carrier airwake introduces complex wind disturbances around the stern, while deck motion changes the relative position of the ITP and directly affects the desired approach path. In addition, gust disturbances may cause abrupt variations in the flight-path angle, heading angle, and angular rate dynamics [5,6]. These factors make the UAV landing problem a strongly coupled tracking-control problem, where the position loop, trajectory loop, attitude loop, and velocity loop must be coordinated under time-varying disturbances.

In landing systems, deck motion information is commonly predicted on the carrier side and transmitted to the UAV for guidance command correction [7]. Typical short-term prediction methods include autoregressive (AR) models [2,8,9], recursive least squares (RLS) [6], and long short-term memory (LSTM) networks [10]. Owing to the inertia characteristic of ship motion, these methods usually provide comparable accuracy over a short prediction horizon. By embedding the predicted ITP motion into the guidance law [2,3,6], the tracking delay caused by deck motion can be alleviated. However, if the predicted ITP sequence is directly used as a moving reference, rapid changes in the deck motion trend may introduce aggressive guidance commands and induce undesirable fluctuations in the trajectory and attitude responses. Therefore, the predicted sequence should be integrated into the guidance law in a smooth and coordinated manner.

In contrast to reference variations induced by deck motion, carrier airwake and gust disturbances directly affect the UAV dynamics through aerodynamic force and moment perturbations [3,5,6,8]. Existing studies usually address these disturbances under nonlinear dynamic inversion (NDI) or backstepping frameworks [2,6,11,12], where robust control techniques, such as sliding-mode control [2,5,13–16], are introduced to enhance disturbance rejection. Disturbance observers are further implemented in many designs to provide feedforward compensation, thereby reducing the need for overly conservative robust gains [2,5,8,17,18]. However, carrier airwake and especially gust disturbances are unsteady, stochastic, and rapidly varying, which imposes stricter requirements on the transient tracking accuracy and phase response of the disturbance observer.

Direct force control provides an effective way to reduce the coupling between trajectory regulation and attitude maneuvering for aircraft [19,20]. Instead of relying only on pitch attitude to reshape the flight path, direct-force effectors can generate incremental lift more directly, thereby improving response bandwidth and reducing controller workload [12]. This idea has been validated in practical carrier-landing applications through the MAGIC CARPET program [4], where integrated direct lift control and delta flight-path command

were implemented on carrier-based aircraft to simplify glide-slope correction and improve touchdown consistency.

However, existing direct-force landing studies mainly focus on the longitudinal channel, especially direct lift control [2,5,12,21]. The lateral channel is still commonly handled through bank-angle/yaw-angle and sideslip-mediated dynamics. In the ATARI project [22], the delta-command concept was extended to the lateral direction to make lineup correction more intuitive, but the lateral motion was still produced mainly through conventional control surfaces and attitude-coupled dynamics. Nevertheless, the aircraft attitude itself must also be carefully maintained for touchdown geometry, arresting-hook engagement, and impact load reduction. Therefore, introducing a direct side-force channel is meaningful for carrier-based UAV landing.

From the convergence perspective, conventional asymptotic control can guarantee stability only over an infinite time horizon, which is not fully consistent with the strict time window of carrier landing [23,24]. Finite-time control improves the transient response by driving the tracking error to zero within a finite interval, but the settling time usually depends on the initial condition [13,25]. Fixed-time control removes this dependence by providing a uniform upper bound of the settling time, yet this bound is often implicit or conservative and cannot be directly adjusted according to the landing task [3,26]. Predefined-time control further addresses this limitation by allowing the convergence time to be specified in advance, making it more suitable for terminal approach problems with clear time requirements [2,5,27–29]. The same property is also desirable for disturbance observers [2,5,28,29], since accurate feedforward compensation must be established rapidly when airwake or gust disturbances enter the UAV dynamics. Recent learning-based studies have also investigated flexible constraint handling and adjustable convergence behavior [30,31]. However, enforcing fast convergence within a predefined time interval may lead to aggressive transient commands, which makes actuator constraints and anti-saturation design [14,32] an important issue in practical landing control.

To address the above issues, this paper develops a predefined-time integrated direct lift/side-force landing-control scheme for carrier-based fixed-wing UAVs. The main contributions are summarized as follows:

1. An integrated direct lift/side-force control architecture is developed for carrier landing. Existing direct-force landing studies mainly introduce a longitudinal direct-lift channel, while lateral correction is still commonly achieved through bank-angle- and sideslip-mediated motion. In contrast, the proposed architecture employs the trailing-edge flap and a differential-spoiler-based side-force channel for vertical and lateral trajectory correction, respectively, reducing reliance on attitude-mediated motion.
2. A unified practical predefined-time control framework is established for the cascaded position, trajectory, attitude, angular-rate, and velocity loops. In contrast to asymptotic and finite-time methods, the proposed design allows an upper bound on the convergence time to be specified in advance. Prescribed-performance constraints and predefined-time disturbance observation are incorporated into the same framework to regulate the attitude transient and compensate for the lumped disturbances.
3. An ITP-preview guidance strategy and a trailing-edge-flap anti-saturation mechanism are incorporated. The predicted ITP sequence is smoothly integrated into the guidance law, while the anti-saturation mechanism compensates for flap saturation and mitigates its coupled influence on the longitudinal channel.

The remainder of this paper is organized as follows. Section 2 formulates the carrier-based UAV landing problem and defines the control objectives. Section 3 introduces the theoretical tools used for controller, observer, and anti-saturation design. Section 4 presents

the proposed landing control scheme. Section 5 analyzes the closed-loop stability. Section 6 validates the proposed method through simulation studies. Section 7 concludes this paper.

2. Problem Formulation

This section formulates the carrier-landing problem for a fixed-wing UAV under deck motion and wind disturbances.

2.1. UAV Model

The inertial frame $O_g x_g y_g z_g$ and the body-fixed frame $O_b x_b y_b z_b$ are considered. The body-fixed frame is attached to the UAV, with its origin at the center of mass. The x_b -axis points forward, the y_b -axis points to the right wing, and the z_b -axis points downward. The UAV configuration and the adopted control surfaces are illustrated in Figure 1.

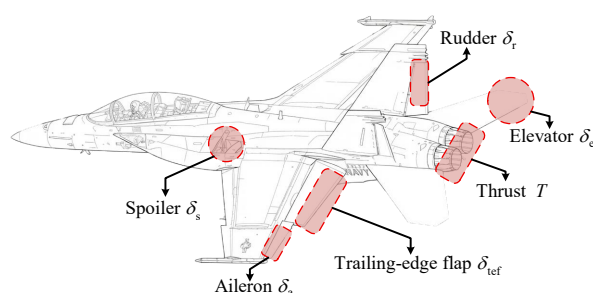


Figure 1. UAV configuration and control surfaces.

The high-fidelity simulation is based on the nonlinear six-degree-of-freedom (six-DOF) fixed-wing UAV model [33,34], whose main equations are summarized in Appendix A. For controller synthesis, the nonlinear dynamics are reorganized into a cascade affine form. The state variables are grouped as $x_1 = [y, z]^T$, $x_2 = [\chi, \gamma]^T$, $x_3 = [\mu, \alpha, \beta]^T$, and $x_4 = [p, q, r]^T$, where y and z denote the lateral and vertical positions, χ is the heading angle, γ is the flight-path angle, μ is the bank angle, α is the angle of attack, β is the sideslip angle, and p , q , and r are the roll rate, pitch rate, and yaw rate, respectively.

The control-oriented affine model is written as

$$\begin{aligned}\dot{x}_1 &= f_1(x_2, V) + g_1(V)x_2, \\ \dot{x}_2 &= f_2(x_2, x_3, V) + g_2(x_2, x_3, V)u_2 + d_2, \\ \dot{x}_3 &= f_3(x_2, x_3) + g_3(x_3)x_4, \\ \dot{x}_4 &= f_4(x_3, x_4, V) + g_4(x_3, V)u_4 + d_4, \\ \dot{V} &= f_V(x_2, x_3) + g_V(x_3)T + d_V,\end{aligned}\tag{1}$$

where V is the velocity, T is the thrust command, $x = [x_1^T, x_2^T, x_3^T, x_4^T, V]^T$ is the compact state vector, $u_2 = [\delta_s, \delta_{tef}]^T$ and $u_4 = [\delta_a, \delta_e, \delta_r]^T$ are the control inputs, $f_i(\cdot)$, $g_i(\cdot)$, $f_V(\cdot)$, and $g_V(\cdot)$ are known nonlinear functions derived from the six-DOF model, and $d_2 = [d_\chi, d_\gamma]^T$, $d_4 = [d_p, d_q, d_r]^T$, and d_V denote lumped disturbances in the trajectory, angular rate, and velocity loops, respectively.

Assumption 1 ([33]). Let $\mathcal{I}_d = \{\chi, \gamma, p, q, r, V\}$ collect the six lumped-disturbance channels associated with carrier airwake, gusts, and unmodeled aerodynamic effects. In addition to having finite amplitudes, these disturbances are assumed to vary at finite rates. Specifically, each $d_j(t)$, $j \in \mathcal{I}_d$, is continuously differentiable during the landing interval, and there exist constants $\bar{d}_j > 0$ and $\bar{v}_j > 0$ such that $|d_j(t)| \leq \bar{d}_j$ and $|\dot{d}_j(t)| \leq \bar{v}_j$ throughout that interval.

Assumption 2. The input gain matrices or scalars used in the cascade loops, including g_1 , g_2 , g_3 , g_4 , and g_V , are nonsingular or bounded away from zero within the considered landing envelope.

In the position loop, $x_2 = [\chi, \gamma]^\top$ is regarded as the virtual control input for regulating $x_1 = [y, z]^\top$. In the trajectory loop, the direct-force input is selected as $u_2 = [\delta_s, \delta_{\text{tef}}]^\top$, where δ_s denotes the differential spoiler deflection, and δ_{tef} denotes the trailing-edge flap deflection. This spoiler-based direct side-force channel follows the DSFC concept in Cao et al. [34], where spoiler deflection is used to generate lateral force while reducing the dependence on bank-angle and sideslip motion. The attitude loop treats $x_4 = [p, q, r]^\top$ as the virtual control input for $x_3 = [\mu, \alpha, \beta]^\top$. The angular rate loop generates the conventional control-surface commands $u_4 = [\delta_a, \delta_e, \delta_r]^\top$, where δ_a , δ_e , and δ_r denote the aileron, elevator, and rudder deflections, respectively. The velocity loop is independently regulated by thrust T . With the direct-force channels incorporated into the trajectory loop, the direct lift/side-force control architecture is formed.

Figure 2 further illustrates the control signal flow of this architecture. To facilitate interpretation of the schematic, ϕ , θ , and ψ denote the roll, pitch, and yaw Euler angles, respectively; $G_p(s)$ represents the engine dynamics; $\bar{Q}$ is the dynamic pressure; and coefficients of the form C denote the corresponding aerodynamic stability or control derivatives. The terms α_w and β_w represent the wind-induced equivalent angle-of-attack and sideslip perturbations, while arrows marked by $\int$ indicate time integration along the corresponding dynamic paths. The remaining state, control, force, and moment symbols are defined above and summarized in Table A1, with the detailed aerodynamic and kinematic relations provided in Appendix A. The direct-force paths provide additional force-generation channels for trajectory correction, so that vertical and lateral position errors can be reduced with less reliance on angle-of-attack- and bank-angle/sideslip-mediated correction. In the conventional longitudinal path, vertical correction is transmitted through

$$\delta_e \rightarrow \Delta \bar{M} \rightarrow \dot{q} \xrightarrow{\int} q \rightarrow \dot{\alpha} \xrightarrow{\int} \Delta \alpha \rightarrow \Delta L(\alpha) \rightarrow \dot{\gamma} \xrightarrow{\int} \Delta \gamma \rightarrow \dot{z} \xrightarrow{\int} \Delta z. \quad (2)$$

By contrast, the direct-lift path can be represented as

$$\delta_{\text{tef}} \rightarrow \Delta L \rightarrow \dot{\gamma} \xrightarrow{\int} \Delta \gamma \rightarrow \dot{z} \xrightarrow{\int} \Delta z. \quad (3)$$

Thus, the trailing-edge flap can rapidly increase or decrease aerodynamic lift while keeping the angle-of-attack nearly unchanged, which bypasses the elevator-induced non-minimum-phase response, broadens the trajectory response bandwidth, and partially decouples trajectory motion from attitude motion. Similarly, the lateral channel is shortened from the conventional attitude-coupled path

$$\delta_a / \delta_r \rightarrow \Delta \bar{L}, \Delta \bar{N} \rightarrow \dot{p} / \dot{r} \xrightarrow{\int} p / r \xrightarrow{\int} \phi / \psi, \beta \rightarrow \dot{\chi} \xrightarrow{\int} \Delta \chi \rightarrow \dot{y} \xrightarrow{\int} \Delta y \quad (4)$$

to the direct side-force path

$$\delta_s \rightarrow \Delta Y \rightarrow \dot{\chi} \xrightarrow{\int} \Delta \chi \rightarrow \dot{y} \xrightarrow{\int} \Delta y. \quad (5)$$

Remark 1. For clarity, the conventional lateral path in Figure 2 and Equation (4) is represented by the sideslip-induced side-force mechanism, i.e., $\beta \rightarrow Y \rightarrow \dot{\chi}$, with $\sin \mu \approx 0$ and $\cos \mu \approx 1$. In the full nonlinear model, the bank-angle-induced lift/thrust projection and the associated bank-angle coupling terms are retained, as shown in Appendix A.

2.2. Environmental Modeling

The carrier airwake is modeled according to the MIL-F-8785C standard [35], and the airwake velocity in the body-axis frame is denoted by $\mathbf{V}_{aw} = [u_{aw}, v_{aw}, w_{aw}]^\top$. Following the standard description, the carrier airwake is composed of free atmospheric turbulence, steady wake, periodic components, and stochastic components. Gusts are additionally introduced to evaluate transient disturbance rejection. The gust vector is denoted by $\mathbf{V}_g = [V_{g,x}, V_{g,y}, V_{g,z}]^\top$, where the i th component is given by $V_{g,i}(t) = A_i \sigma_i(t)$. Here, A_i is the gust amplitude, $t_{g,i}$ is the injection time, $T_{r,i}$ and $T_{f,i}$ are the rise and fall times, and $T_{d,i}$ is the total duration. With $\tau_i = t - t_{g,i}$, the profile function is defined as

$$\sigma_i(t) = \begin{cases} 0, & \tau_i < 0, \\ \frac{1}{2} \left[1 - \cos \left(\frac{\pi \tau_i}{T_{r,i}} \right) \right], & 0 \leq \tau_i < T_{r,i}, \\ 1, & T_{r,i} \leq \tau_i < T_{d,i} - T_{f,i}, \\ \frac{1}{2} \left[1 + \cos \left(\frac{\pi (\tau_i - T_{d,i} + T_{f,i})}{T_{f,i}} \right) \right], & T_{d,i} - T_{f,i} \leq \tau_i \leq T_{d,i}, \\ 0, & \tau_i > T_{d,i}. \end{cases} \quad (6)$$

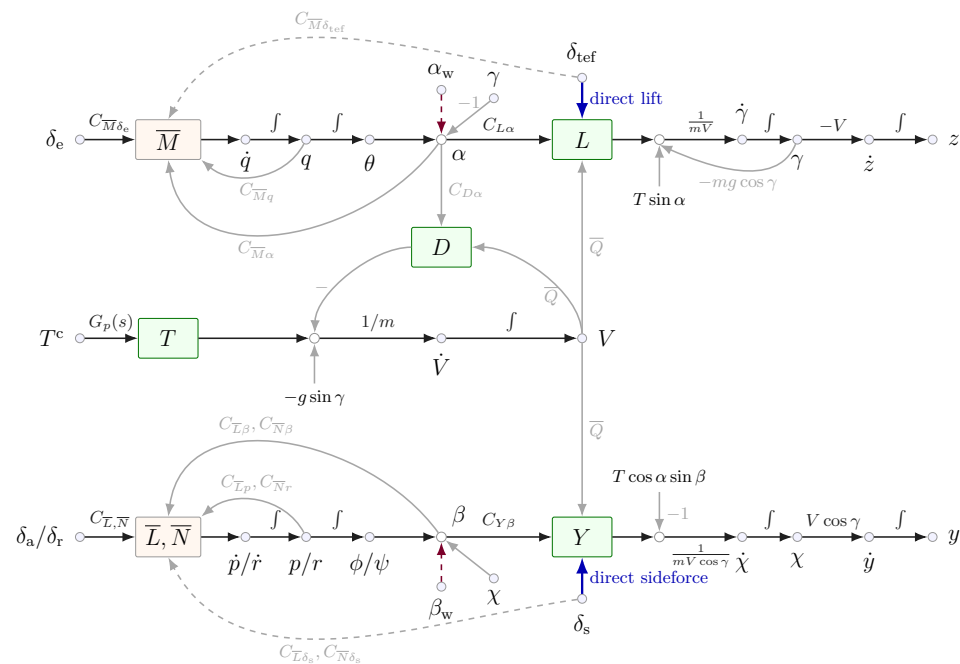


Figure 2. Control signal flow of the integrated direct lift/side-force architecture.

The total wind disturbance is then expressed as $\mathbf{V}_w = \mathbf{V}_{aw} + \mathbf{V}_g$.

The origin of $O_g x_g y_g z_g$ coincides with the nominal ITP at $t = 0$. To describe the ITP displacement caused by deck motion, a ship-fixed frame $O_s x_s y_s z_s$ is introduced. Let $\mathbf{r}_{ITP}$ denote the fixed vector from the ship reference point to the ITP in the ship-fixed frame. The rotation matrix from the ship-fixed frame to the inertial frame is

$$\mathbf{R}_{gs} = \begin{bmatrix} \cos \theta_s \cos \psi_s & \sin \phi_s \sin \theta_s \cos \psi_s - \cos \phi_s \sin \psi_s & \cos \phi_s \sin \theta_s \cos \psi_s + \sin \phi_s \sin \psi_s \\ \cos \theta_s \sin \psi_s & \sin \phi_s \sin \theta_s \sin \psi_s + \cos \phi_s \cos \psi_s & \cos \phi_s \sin \theta_s \sin \psi_s - \sin \phi_s \cos \psi_s \\ -\sin \theta_s & \sin \phi_s \cos \theta_s & \cos \phi_s \cos \theta_s \end{bmatrix}, \quad (7)$$

where ϕ_s , θ_s , and ψ_s are the ship roll, pitch, and yaw angles, respectively. The instantaneous ITP position in the inertial frame is written as

$$\mathbf{P}_{\text{ITP}}(t) = \mathbf{P}_{\text{ITP}}^0(t) + \mathbf{P}_{\text{trans}}(t) + [\mathbf{R}_{\text{gs}}(\phi_s, \theta_s, \psi_s) - \mathbf{I}_3] \mathbf{r}_{\text{ITP}}, \quad (8)$$

where $\mathbf{P}_{\text{ITP}}^0(t)$ denotes the nominal ITP trajectory associated with carrier translation and satisfies $\mathbf{P}_{\text{ITP}}^0(0) = \mathbf{0}$, $\mathbf{P}_{\text{trans}}(t)$ denotes the translational deck motion due to surge, sway, and heave, and $\mathbf{I}_3$ is the identity matrix. In the guidance loop, the short-term ITP displacement sequence is predicted using an autoregressive (AR) model [2,8] and incorporated into the reference generation. The predictor settings are listed in Table A7.

2.3. Control Objectives

Figure 3 illustrates the carrier-landing process considered in this paper. The UAV is required to approach the deck-relative glide slope and converge to the neighborhood of the ITP under carrier airwake, gust disturbances, and deck motion.

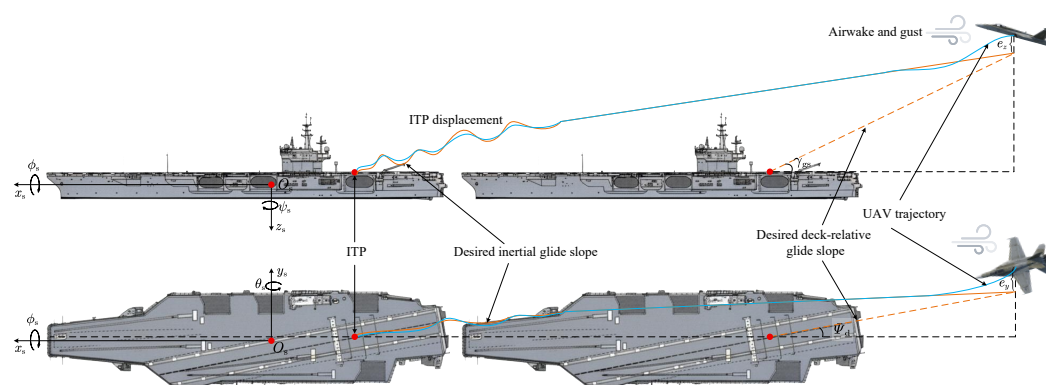


Figure 3. Carrier-landing process.

With $L_x(t) = P_{\text{ITP},x}(t) - x(t)$, the desired glide slope is defined as

$$\begin{cases} y^c(t) = L_x(t) \tan \psi_d + P_{\text{ITP},y}(t), \\ z^c(t) = \frac{L_x(t) \tan(-\gamma_{\text{gs}})}{\cos \psi_d} + P_{\text{ITP},z}(t), \end{cases} \quad (9)$$

where ψ_d is the deck-relative approach angle, and γ_{gs} is the nominal glide-slope angle. The lateral and vertical tracking errors are $e_y = y - y^c$ and $e_z = z - z^c$, respectively.

The control objective is to drive e_y and e_z into a small neighborhood of zero within a predefined time, while keeping the attitude errors of μ , α , and β inside prescribed performance bounds. Meanwhile, the angular rates and velocity should remain bounded under wind disturbances and actuator constraints. To achieve these objectives, the following sections develop a predefined-time direct lift/side-force controller, a predefined-time disturbance observer, and an anti-saturation mechanism for the direct-lift channel.

3. Preliminaries

3.1. Predefined-Time Convergence

Several preliminary lemmas on predefined-time convergence and stability analysis are introduced in this subsection.

Lemma 1 ([29]). Let $x = 0$ be an equilibrium point of the nonlinear dynamics $\dot{x} = f(x, t)$ with $x \in \mathbb{R}^n$. Assume that a continuously differentiable positive definite Lyapunov function $V(x)$ is

available, satisfying $V(0) = 0$ and $V(x) > 0$ for all $x \neq 0$. If, along the system trajectory, there exist constants $m > 0$, $n > 0$, $T_p > 0$, $\varphi \in (0, 1)$, and $\omega \geq 0$ such that

$$\dot{V} \leq -\frac{\pi}{\varphi\sqrt{mn}T_p} \left(mV^{1-\frac{\varphi}{2}} + nV^{1+\frac{\varphi}{2}} \right) + \omega, \quad (10)$$

it follows that the system state is practically predefined-time stable. More specifically, for any $\vartheta \in (0, 1)$, the trajectory $x(t)$ reaches

$$\Omega_x = \left\{ x \in \mathbb{R}^n : V(x) \leq \min \left[\left(\frac{\omega\varphi\sqrt{mn}T_p}{\pi m(1-\vartheta)} \right)^{\frac{2}{2-\varphi}}, \left(\frac{\omega\varphi\sqrt{mn}T_p}{\pi n(1-\vartheta)} \right)^{\frac{2}{2+\varphi}} \right] \right\} \quad (11)$$

no later than $T_p/\sqrt{\vartheta}$, and this time bound is independent of the initial condition. In the disturbance-free case $\omega = 0$, Ω_x collapses to the origin, which implies predefined-time convergence of $x(t)$ to zero.

Lemma 2 ([2]). For nonnegative scalars x_i , $i = 1, 2, \dots, N$, and $\lambda > 0$, the following inequalities hold:

$$\left(\sum_{i=1}^N x_i \right)^\lambda \leq \sum_{i=1}^N x_i^\lambda, \quad 0 < \lambda \leq 1, \quad \sum_{i=1}^N x_i^\lambda \geq N^{1-\lambda} \left(\sum_{i=1}^N x_i \right)^\lambda, \quad \lambda \geq 1. \quad (12)$$

Lemma 3 ([36]). For any $a \in \mathbb{R}$ and $b > 0$, the smooth hyperbolic-tangent approximation satisfies

$$0 \leq |a| - a \tanh\left(\frac{a}{b}\right) \leq 0.2785b. \quad (13)$$

To facilitate numerical evaluation near the origin, define the regularized Lyapunov variable $V_\epsilon = \max\{V, \epsilon\}$, where $\epsilon > 0$ is a small positive threshold. Thus, $V_\epsilon = V$ for $V \geq \epsilon$, whereas $V_\epsilon = \epsilon$ for $0 \leq V < \epsilon$.

3.2. Prescribed Performance

Prescribed performance control (PPC) is adopted to impose explicit transient and steady-state bounds on attitude tracking errors. For a generic error e_i , the prescribed envelope is written as $-\underline{\delta}_i\rho_i(t) < e_i(t) < \bar{\delta}_i\rho_i(t)$, where $\underline{\delta}_i > 0$ and $\bar{\delta}_i > 0$ determine the lower and upper bounds, respectively. The positive function $\rho_i(t)$ decreases from its initial value to a small positive constant, thereby specifying both the transient allowable range and the final tracking accuracy.

Let $\zeta_i = e_i/\rho_i(t)$ denote the normalized error. Then, the PPC constraint is equivalent to $-\underline{\delta}_i < \zeta_i < \bar{\delta}_i$. To convert the constrained error into an unconstrained variable, the following logarithmic transformation is used:

$$\varepsilon_i = \frac{1}{2} \ln \left(\frac{\bar{\delta}_i + \zeta_i}{\underline{\delta}_i - \zeta_i} \right). \quad (14)$$

Therefore, boundedness of ε_i guarantees that e_i remains inside the prescribed-performance envelope, while predefined-time convergence of ε_i further provides the desired convergence time property for the attitude loop error.

Assumption 3. The initial attitude errors satisfy the prescribed-performance feasibility condition, namely, $L_{3j}(0) < e_{3j}(0) < U_{3j}(0)$ for $j \in \{\mu, \alpha, \beta\}$.

Remark 2. The logarithmic prescribed-performance transformation is well defined only for $-\delta_i < \zeta_i < \bar{\delta}_i$. The transformed error becomes unbounded as ζ_i approaches either boundary and undefined once a boundary is violated, which constitutes the singularity issue in PPC. In this study, the preceding feasibility assumption ensures initial well-posedness, while the subsequent stability analysis guarantees that the prescribed-performance region is preserved under the stated conditions. If disturbances or actuator limitations make a fixed performance envelope temporarily infeasible, flexible or readjustable boundaries can be introduced to maintain transformation feasibility [37,38].

3.3. Observer Design

Consider a general affine nonlinear system with a lumped disturbance,

$$\dot{x} = f(x) + g(x)u + d, \quad (15)$$

where $x \in \mathbb{R}^n$, $u \in \mathbb{R}^m$, and $d \in \mathbb{R}^n$ denote the state, control input, and lumped disturbance, respectively. Let $\hat{x}$ and $\hat{d}$ be the estimates of x and d , and define the observation error and disturbance estimation error as $\tilde{x} = x - \hat{x}$ and $\tilde{d} = d - \hat{d}$, respectively.

The predefined-time disturbance observer (PTDO) is given by

$$\begin{aligned} \dot{\hat{x}} &= f(\hat{x}) + g(\hat{x})u + \hat{d} + K_o(\tilde{x})\tilde{x}, \\ \dot{\hat{d}} &= K_d K_o^2(\tilde{x})\tilde{x}, \end{aligned} \quad (16)$$

where $K_d = \text{diag}\{k_{d,1}, \dots, k_{d,n}\} \succ 0$. Equations (15) and (16) give the coupled-error dynamics $\dot{\tilde{x}} = \tilde{d} - K_o(\tilde{x})\tilde{x}$ and $\dot{\tilde{d}} = \tilde{d} - K_d K_o^2(\tilde{x})\tilde{x}$. The observer gain is applied componentwise and selected as

$$\begin{aligned} K_o(\tilde{x}) &= \text{diag}\{k_{o,1}(\tilde{x}_1), \dots, k_{o,n}(\tilde{x}_n)\}, \\ k_{o,j}(\tilde{x}_j) &= k_{l,j} + k_{s,j} \frac{\pi}{2\varphi\sqrt{mn}T_o} \left(mV_{o,j,\epsilon}^{-\varphi/2} + nV_{o,j,\epsilon}^{\varphi/2} \right), \end{aligned} \quad (17)$$

where $V_{o,j} = \tilde{x}_j^2/2$, $V_{o,j,\epsilon} = \max\{V_{o,j}, \epsilon_{o,j}\}$, $k_{l,j} > 0$, $k_{s,j} > 0$, $T_o > 0$, $m > 0$, $n > 0$, and $\varphi \in (0, 1)$. Thus, $k_{o,j}(\tilde{x}_j)$ has the positive lower bound $\underline{k}_{o,j} = k_{l,j}$. The aggregate state-observation Lyapunov function is $V_o = \sum_{j=1}^n V_{o,j}$. Under Assumption 1, the state observation error is practically predefined-time stable, while the disturbance estimation error is ultimately bounded by a residual that depends on the disturbance variation rate. The coupled-error derivation is provided in Section 5.

3.4. Anti-Saturation

Predefined-time control may require relatively large transient control effort, especially when the initial tracking error is large or rapid disturbance rejection is demanded. In practical carrier landing, however, the control surfaces and propulsion system are subject to physical limits. When the commanded input exceeds the available actuator range, the actual input cannot exactly follow the command, and the resulting input mismatch may degrade the predefined-time tracking performance. Therefore, an anti-saturation mechanism is introduced to compensate for the saturation effect.

For a control input u^c , the actual saturated input is expressed as $u = \text{sat}(u^c)$, where $\text{sat}(\cdot)$ denotes the componentwise saturation function. The saturation error is defined as $\Delta u = u - u^c$. For an affine subsystem $\dot{x} = f(x) + g(x)u + d$, the saturated input can be rewritten as

$$\dot{x} = f(x) + g(x)u^c + g(x)\Delta u + d. \quad (18)$$

Thus, actuator saturation acts as an additional matched input error in the closed-loop dynamics.

Assumption 4. The saturation errors induced by the physical control-surface and thrust limits are bounded within the considered landing envelope.

To reduce the influence of this mismatch, an auxiliary anti-saturation variable η_{as} is introduced as

$$\dot{\eta}_{as} = -\frac{k_1\pi}{vT_{as}} \text{sig}^{1-v}(\eta_{as}) - \frac{k_2\pi}{vT_{as}} \text{sig}^{1+v}(\eta_{as}) - k_3 \text{sgn}(\eta_{as}) + G_{as}\Delta u, \quad (19)$$

where $0 < v < 1$, $T_{as} > 0$, $k_3 \geq 0$, and G_{as} is the compensation gain matrix. The constants k_1 and k_2 are selected according to the predefined-time convergence form. According to Lemma 1, if the saturation error is bounded, η_{as} can be driven into a small residual region within $T_{as}/\sqrt{\theta}$.

The auxiliary signal is incorporated into the controller design to compensate for the saturation-induced mismatch. As a result, the closed-loop system can preserve practical predefined-time stability under actuator constraints.

4. Controller Design

The overall automatic carrier landing system (ACLS) architecture is shown in Figure 4, where guidance, command filtering, predefined-time control, PTDO compensation, and anti-saturation modules are integrated to achieve accurate carrier landing under deck motion and wind disturbances.

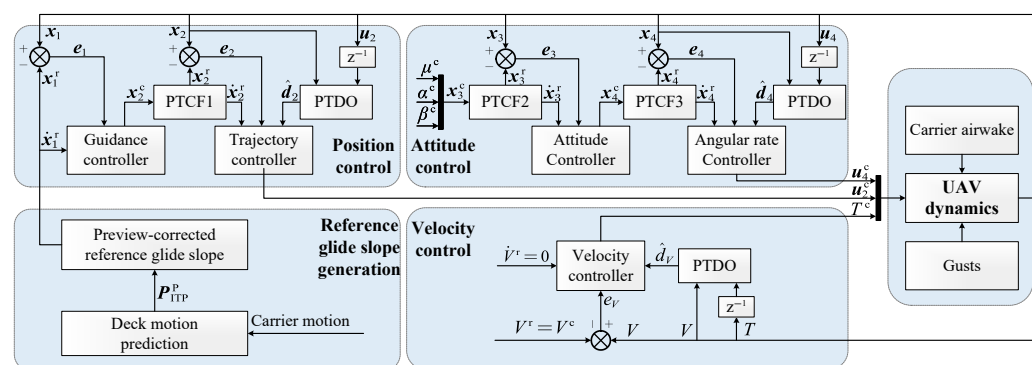


Figure 4. ACLS architecture.

4.1. Reference Glide Slope Generation

The nominal deck-relative glide slope is given in Equation (9). In the controller implementation, the short-term ITP sequence predicted from the deck motion is further used to improve the reference generation. Let the preview-corrected ITP position be

$$P_{ITP}^p(t) = P_{ITP}(t) + \Delta P_{pv}(t), \quad (20)$$

where $\Delta P_{pv}(t)$ denotes the preview correction. Since the vertical deck displacement is more pronounced during the terminal approach, the preview correction is mainly applied to the vertical ITP component, i.e., $\Delta P_{pv}(t) = [0, 0, \beta_{pv}(t)(P_{ITP,z}(t + T_{pv}) - P_{ITP,z}(t))]^\top$, where $T_{pv} > 0$ is the preview horizon, and $\beta_{pv}(t)$ is a bounded preview weight adjusted according to the predicted deck-motion trend.

By replacing $P_{ITP}(t)$ in Equation (9) with $P_{ITP}^p(t)$, the position command $x_1^c = [y^c, z^c]^\top$ is obtained. Its derivative is calculated from the ITP velocity and the UAV approach velocity along the inertial x -axis. The generated reference glide slope is then supplied to the position control loop.

Assumption 5. The reference commands generated in the cascade control design and the derivatives required by the command filters and feedback laws are bounded.

4.2. Position Control

4.2.1. Position Outer Loop Design (Guidance Control)

The position outer loop generates the trajectory-angle command for lateral and vertical regulation. Based on Equation (1), the position dynamics can be written in the affine form

$$\dot{x}_1 = f_1(x_2, V) + g_1(V)x_2, \quad (21)$$

where $x_1 = [y, z]^\top$, $x_2 = [\chi, \gamma]^\top$, and

$$f_1 = \begin{bmatrix} V(\cos \gamma \sin \chi - \chi) \\ -V(\sin \gamma - \gamma) \end{bmatrix}, \quad g_1 = \begin{bmatrix} V & 0 \\ 0 & -V \end{bmatrix}.$$

The reference glide-slope generation provides the position command $x_1^c = [y^c, z^c]^\top$. In general, a command filter can be used to obtain a smooth reference signal. Since the position command varies slowly during the final approach, this loop directly sets $x_1^r = x_1^c$ and $\dot{x}_1^r = \dot{x}_1^c$. Define the position tracking error as $e_1 = x_1 - x_1^r$.

To derive the guidance command, the desired position-error dynamics is assigned as $\dot{e}_1^{\text{des}} = -K_1^{\text{pt}} e_1$. The gain matrix is selected according to the predefined-time convergence form in Lemma 1, namely, $K_1^{\text{pt}} = \text{diag}\{k_{1y}^{\text{pt}}, k_{1z}^{\text{pt}}\}$, with

$$k_{1j}^{\text{pt}} = \frac{\pi}{2\varphi_{1j}\sqrt{m_{1j}n_{1j}T_{1j}}} \left(m_{1j}V_{1j,\epsilon}^{-\varphi_{1j}/2} + n_{1j}V_{1j,\epsilon}^{\varphi_{1j}/2} \right), \quad j \in \{y, z\}.$$

Here, $V_{1j} = e_{1j}^2/2$ is the candidate Lyapunov function of the j th position error, and $m_{1j} > 0$, $n_{1j} > 0$, $\varphi_{1j} > 0$, and $T_{1j} > 0$ are design parameters.

The desired derivative of x_1 is then obtained by $\dot{x}_1^{\text{des}} = \dot{x}_1^r - K_1^{\text{pt}} e_1$. Substituting $\dot{x}_1^{\text{des}}$ into Equation (21) gives the trajectory-angle command

$$x_2^c = g_1^{-1}(V) \left[\dot{x}_1^r - K_1^{\text{pt}} e_1 - f_1(x_2, V) \right], \quad (22)$$

where $x_2^c = [\chi^c, \gamma^c]^\top$.

4.2.2. Position Inner Loop Design (Trajectory Control)

The trajectory loop tracks the heading and flight-path angle references generated by the guidance loop. To obtain smooth reference signals, the command x_2^c is passed through a predefined-time command filter (PTCF1) using

$$\dot{x}_2^r = \dot{x}_2^c - K_{f2}^{\text{pt}} e_{f2}, \quad e_{f2} = x_2^r - x_2^c, \quad (23)$$

where $K_{f2}^{\text{pt}} = \text{diag}\{k_{f2,\chi}^{\text{pt}}, k_{f2,\gamma}^{\text{pt}}\}$. For $j \in \{\chi, \gamma\}$, the filter gain is selected as

$$k_{f2,j}^{\text{pt}} = \frac{\pi}{2\varphi_{f2,j}\sqrt{m_{f2,j}n_{f2,j}T_{f2,j}}} \left(m_{f2,j}V_{f2,j,\epsilon}^{-\varphi_{f2,j}/2} + n_{f2,j}V_{f2,j,\epsilon}^{\varphi_{f2,j}/2} \right),$$

where $V_{f2,j} = e_{f2,j}^2/2$, and $m_{f2,j} > 0$, $n_{f2,j} > 0$, $\varphi_{f2,j} > 0$, and $T_{f2,j} > 0$ are design parameters.

According to Equation (1), the trajectory dynamics are expressed as

$$\dot{x}_2 = f_2 + g_2 u_2 + d_2, \quad (24)$$

where $\mathbf{x}_2 = [\chi, \gamma]^\top$, $\mathbf{u}_2 = [\delta_s, \delta_{\text{tef}}]^\top$, $\mathbf{d}_2 = [d_\chi, d_\gamma]^\top$, and

$$\mathbf{f}_2 = \begin{bmatrix} \frac{L \sin \mu + Y_{\delta_s} \cos \mu + T(\sin \alpha \sin \mu - \cos \alpha \sin \beta \cos \mu)}{mV \cos \gamma} \\ \frac{T(\sin \alpha \cos \mu + \cos \alpha \sin \beta \sin \mu) - Y \sin \mu - mg \cos \gamma + L_{\delta_{\text{tef}}} \cos \mu}{mV} \end{bmatrix},$$

$$\mathbf{g}_2 = \begin{bmatrix} \frac{\bar{Q}SC_{Y\delta_s} \cos \mu}{mV \cos \gamma} & 0 \\ 0 & \frac{\bar{Q}SC_{L\delta_{\text{tef}}} \cos \mu}{mV} \end{bmatrix}.$$

Here, $Y_{\delta_s} = Y - \bar{Q}SC_{Y\delta_s} \delta_s$ and $L_{\delta_{\text{tef}}} = L - \bar{Q}SC_{L\delta_{\text{tef}}} \delta_{\text{tef}}$ denote the side force and lift after removing the direct-force input contributions, respectively.

The anti-saturation state is introduced into the flight-path reference to compensate for the trailing-edge-flap realization error. Let $\mathbf{b}_\gamma = [0, 1]^\top$ and let $\eta_{\text{as}, \gamma}$ denote the flight-path component generated by Equation (19). The compensated trajectory reference is defined as

$$\mathbf{x}_{2,\text{as}}^\tau = \mathbf{x}_2^\tau + \mathbf{b}_\gamma \eta_{\text{as}, \gamma}, \quad \gamma_{\text{as}}^\tau = \gamma^\tau + \eta_{\text{as}, \gamma}. \quad (25)$$

The nominal filtered derivative $\dot{\mathbf{x}}_2^\tau$ is retained in the control command, while the auxiliary-state dynamics compensates for the saturation mismatch in the resulting error system.

The auxiliary state is applied to the flap channel because its realization error acts directly through the flight-path-angle dynamics on vertical-position tracking, while all remaining actuator realization errors are retained in the composite stability analysis.

Define the trajectory tracking error as $\mathbf{e}_2 = \mathbf{x}_2 - \mathbf{x}_{2,\text{as}}^\tau$. The desired trajectory-error dynamics is assigned as $\dot{\mathbf{e}}_2^{\text{des}} = -\mathbf{K}_2^{\text{pt}} \mathbf{e}_2$, where $\mathbf{K}_2^{\text{pt}} = \text{diag}\{k_{2\chi}^{\text{pt}}, k_{2\gamma}^{\text{pt}}\}$. For $j \in \{\chi, \gamma\}$,

$$k_{2j}^{\text{pt}} = \frac{\pi}{2\varphi_{2j} \sqrt{m_{2j} n_{2j} T_{2j}}} \left(m_{2j} V_{2j,\epsilon}^{-\varphi_{2j}/2} + n_{2j} V_{2j,\epsilon}^{\varphi_{2j}/2} \right),$$

where $V_{2j} = e_{2j}^2/2$, and $m_{2j} > 0$, $n_{2j} > 0$, $\varphi_{2j} > 0$, and $T_{2j} > 0$ are design parameters. Applying the PTDO in Equation (16) to $\dot{\mathbf{x}}_2$ gives the disturbance estimate $\hat{\mathbf{d}}_2$. Then, the direct-force command is designed as

$$\mathbf{u}_2^c = \mathbf{g}_2^{-1} \left[\dot{\mathbf{x}}_2^\tau - \mathbf{K}_2^{\text{pt}} \mathbf{e}_2 - \mathbf{f}_2 - \hat{\mathbf{d}}_2 \right], \quad (26)$$

where $\mathbf{u}_2^c = [\delta_s^c, \delta_{\text{tef}}^c]^\top$.

4.3. Attitude Control

4.3.1. Attitude Outer Loop Design (Attitude Control)

The attitude outer loop generates the angular-rate command for tracking the bank-angle, angle-of-attack, and sideslip-angle references. In the proposed landing controller, the attitude command is specified as $\mathbf{x}_3^c = [\mu^c, \alpha^c, \beta^c]^\top$, where $\mu^c = 0$, $\alpha^c = 8.1^\circ$, and $\beta^c = 0$. The command $\mathbf{x}_3^c$ is passed through a predefined-time command filter, denoted as PTCF2, to obtain the smooth reference $\mathbf{x}_3^\tau$ and its derivative.

$$\dot{\mathbf{x}}_3^\tau = \dot{\mathbf{x}}_3^c - \mathbf{K}_{\text{f3}}^{\text{pt}} \mathbf{e}_{\text{f3}}, \quad \mathbf{e}_{\text{f3}} = \mathbf{x}_3^\tau - \mathbf{x}_3^c. \quad (27)$$

Here, K_{f3}^{pt} has the same predefined-time form as PTCF1. According to Equation (1), the attitude dynamics are written as

$$\dot{x}_3 = f_3 + g_3 x_4, \quad (28)$$

where $x_3 = [\mu, \alpha, \beta]^\top$, $x_4 = [p, q, r]^\top$, and

$$f_3 = \begin{bmatrix} \dot{\gamma} \tan \beta \cos \mu + \dot{\chi} (\sin \gamma + \tan \beta \sin \mu \cos \gamma) \\ -\dot{\gamma} \frac{\cos \mu}{\cos \beta} - \dot{\chi} \frac{\sin \mu \cos \gamma}{\cos \beta} \\ -\dot{\gamma} \sin \mu + \dot{\chi} \cos \mu \cos \gamma \end{bmatrix},$$

$$g_3 = \begin{bmatrix} \frac{\cos \alpha}{\cos \beta} & 0 & \frac{\sin \alpha}{\cos \beta} \\ -\cos \alpha \tan \beta & 1 & -\sin \alpha \tan \beta \\ \sin \alpha & 0 & -\cos \alpha \end{bmatrix}.$$

Define the attitude tracking error as $e_3 = x_3 - x_3^r$. For $j \in \{\mu, \alpha, \beta\}$, the prescribed-performance constraint is imposed as $L_{3j}(t) < e_{3j}(t) < U_{3j}(t)$, where $U_{3j}(t)$ and $L_{3j}(t)$ denote the upper and lower performance boundaries. Let $c_{3j}(t) = (U_{3j}(t) + L_{3j}(t))/2$ and $w_{3j}(t) = (U_{3j}(t) - L_{3j}(t))/2$. The normalized error and transformed error are defined as $\xi_{3j} = (e_{3j} - c_{3j})/w_{3j}$ and $\varepsilon_{3j} = \frac{1}{2} \ln((1 + \xi_{3j})/(1 - \xi_{3j}))$, respectively. Then, the derivative of the transformed error can be expressed as $\dot{\varepsilon}_{3j} = h_{3j} \dot{e}_{3j} + s_{3j}$, where $h_{3j} = 1/((1 - \xi_{3j}^2)w_{3j})$ and $s_{3j} = -(\dot{c}_{3j} + \xi_{3j} \dot{w}_{3j})/((1 - \xi_{3j}^2)w_{3j})$.

Let $\varepsilon_3 = [\varepsilon_{3\mu}, \varepsilon_{3\alpha}, \varepsilon_{3\beta}]^\top$, $H_3 = \text{diag}\{h_{3\mu}, h_{3\alpha}, h_{3\beta}\}$, and $s_3 = [s_{3\mu}, s_{3\alpha}, s_{3\beta}]^\top$. The desired attitude state derivative is therefore given by

$$\dot{x}_3^{\text{des}} = \dot{x}_3^r - H_3^{-1}(s_3 + v_3), \quad (29)$$

where the stabilizing term is selected as $v_3 = K_3^{\text{pt}} \varepsilon_3$, $K_3^{\text{pt}} = \text{diag}\{k_{3\mu}^{\text{pt}}, k_{3\alpha}^{\text{pt}}, k_{3\beta}^{\text{pt}}\}$, and

$$k_{3j}^{\text{pt}} = \frac{\pi}{2\varphi_{3j}\sqrt{m_{3j}n_{3j}T_{3j}}} \left(m_{3j} V_{3j,\varepsilon}^{-\varphi_{3j}/2} + n_{3j} V_{3j,\varepsilon}^{\varphi_{3j}/2} \right), \quad j \in \{\mu, \alpha, \beta\}. \quad (30)$$

Here, $V_{3j} = \varepsilon_{3j}^2/2$, and $m_{3j} > 0$, $n_{3j} > 0$, $\varphi_{3j} > 0$, and $T_{3j} > 0$ are design parameters. Substituting $\dot{x}_3^{\text{des}}$ into the attitude dynamics yields the angular-rate command

$$x_4^c = g_3^{-1} [\dot{x}_3^r - H_3^{-1}(s_3 + v_3) - f_3], \quad (31)$$

where $x_4^c = [p^c, q^c, r^c]^\top$.

4.3.2. Attitude Inner Loop Design (Angular-Rate Control)

The attitude inner loop tracks the angular-rate command and produces the conventional control surface commands. The angular-rate command $x_4^c = [p^c, q^c, r^c]^\top$ is passed through a predefined-time command filter, denoted as PTCF3, to obtain the smooth reference x_4^r and its derivative.

$$\dot{x}_4^r = \dot{x}_4^c - K_{f4}^{\text{pt}} e_{f4}, \quad e_{f4} = x_4^r - x_4^c. \quad (32)$$

Here, K_{f4}^{pt} has the same predefined-time form as PTCF1. According to Equation (1), the angular-rate dynamics are expressed as

$$\dot{x}_4 = f_4 + g_4 u_4 + d_4, \quad (33)$$

where $\mathbf{x}_4 = [p, q, r]^\top$, $\mathbf{u}_4 = [\delta_a, \delta_e, \delta_r]^\top$, $\mathbf{d}_4 = [d_p, d_q, d_r]^\top$, and

$$\mathbf{f}_4 = \begin{bmatrix} c_1qr + c_2pq + c_3\bar{L}_0 + c_4\bar{N}_0 \\ c_5pr + c_6(r^2 - p^2) + c_7\bar{M}_0 \\ -c_2qr + c_8pq + c_4\bar{L}_0 + c_9\bar{N}_0 \end{bmatrix},$$

$$\mathbf{g}_4 = \begin{bmatrix} \bar{Q}Sb(c_3C_{\bar{L}_{\delta_a}} + c_4C_{\bar{N}_{\delta_a}}) & 0 & \bar{Q}Sb(c_3C_{\bar{L}_{\delta_r}} + c_4C_{\bar{N}_{\delta_r}}) \\ 0 & \bar{Q}S\bar{c}c_7C_{\bar{M}_{\delta_e}} & 0 \\ \bar{Q}Sb(c_4C_{\bar{L}_{\delta_a}} + c_9C_{\bar{N}_{\delta_a}}) & 0 & \bar{Q}Sb(c_4C_{\bar{L}_{\delta_r}} + c_9C_{\bar{N}_{\delta_r}}) \end{bmatrix}.$$

In $\mathbf{f}_4$, $\bar{L}_0$, $\bar{M}_0$, and $\bar{N}_0$ denote the known roll, pitch, and yaw moment components excluding the conventional control surface increments. They include the aerodynamic contributions of the flight states, angular rates, trailing-edge flap, and spoiler deflection.

Define the angular-rate tracking error as $\mathbf{e}_4 = \mathbf{x}_4 - \mathbf{x}_4^r$. The desired derivative is designed as

$$\dot{\mathbf{x}}_4^{\text{des}} = \dot{\mathbf{x}}_4^r - \mathbf{K}_4^{\text{pt}} \mathbf{e}_4, \quad (34)$$

where $\mathbf{K}_4^{\text{pt}} = \text{diag}\{k_{4p}^{\text{pt}}, k_{4q}^{\text{pt}}, k_{4r}^{\text{pt}}\}$, and

$$k_{4j}^{\text{pt}} = \frac{\pi}{2\varphi_{4j}\sqrt{m_{4j}n_{4j}T_{4j}}} \left(m_{4j}V_{4j,\epsilon}^{-\varphi_{4j}/2} + n_{4j}V_{4j,\epsilon}^{\varphi_{4j}/2} \right), \quad j \in \{p, q, r\}. \quad (35)$$

Here, $V_{4j} = e_{4j}^2/2$, and $m_{4j} > 0$, $n_{4j} > 0$, $\varphi_{4j} > 0$, and $T_{4j} > 0$ are design parameters. Applying the PTDO in Equation (16) to $\dot{\mathbf{x}}_4$ gives the disturbance estimate $\hat{\mathbf{d}}_4$. Substituting $\dot{\mathbf{x}}_4^{\text{des}}$ into the angular-rate dynamics yields the control-surface command

$$\mathbf{u}_4^c = \mathbf{g}_4^{-1} \left[\dot{\mathbf{x}}_4^r - \mathbf{K}_4^{\text{pt}} \mathbf{e}_4 - \mathbf{f}_4 - \hat{\mathbf{d}}_4 \right], \quad (36)$$

where $\mathbf{u}_4^c = [\delta_a^c, \delta_e^c, \delta_r^c]^\top$.

4.4. Velocity Control

The velocity loop regulates the UAV airspeed by generating the thrust command. In this work, the velocity reference is selected as the trimmed approach velocity, denoted by $V^c = V_0$. Since the approach velocity command is constant in the considered landing task, the reference is directly set as $V^r = V^c$ and $\dot{V}^r = 0$.

According to Equation (1), the velocity dynamics can be written as

$$\dot{V} = f_V + g_V T + d_V, \quad (37)$$

where T is the thrust, d_V denotes the lumped velocity-channel disturbance, and

$$f_V = -\frac{D}{m} - g \sin \gamma, \quad g_V = \frac{\cos \alpha \cos \beta}{m}.$$

Here, D is the aerodynamic drag.

Define the velocity tracking error as $e_V = V - V^r$. The desired velocity-error dynamics is assigned as

$$\dot{e}_V^{\text{des}} = -k_V^{\text{pt}} e_V, \quad (38)$$

where the predefined-time gain is selected as

$$k_V^{\text{pt}} = \frac{\pi}{2\varphi_V\sqrt{m_Vn_VT_V}} \left(m_VV_{V,\epsilon}^{-\varphi_V/2} + n_VV_{V,\epsilon}^{\varphi_V/2} \right). \quad (39)$$

Here, $V_V = e_V^2/2$, and $m_V > 0$, $n_V > 0$, $\varphi_V > 0$, and $T_V > 0$ are design parameters.

Applying the PTDO in Equation (16) to $\dot{V}$ gives the disturbance estimate $\hat{d}_V$. Then, the desired velocity derivative is obtained as

$$\dot{V}^{\text{des}} = \dot{V}^r - k_V^{\text{pt}} e_V.$$

Substituting $\dot{V}^{\text{des}}$ into Equation (37) yields the thrust command

$$T^c = g_V^{-1} \left(\dot{V}^r - k_V^{\text{pt}} e_V - f_V - \hat{d}_V \right). \quad (40)$$

5. Stability Analysis

This section establishes the practical predefined-time stability of the complete closed-loop system by considering the cascade tracking errors, command-filter errors, disturbance-observer errors, and anti-saturation dynamics together.

Theorem 1. Consider the UAV landing system in Equation (1) under Assumptions 1–5. Let the channel-wise coupling coefficients q_ℓ be defined by Equation (57). Suppose that the observer damping margins satisfy $c_{oi} > 0$ and that the controller, PTDF, and anti-saturation parameters satisfy the explicit sufficient conditions in Equation (58). Then, all closed-loop signals are bounded. Moreover, the complete augmented error reaches the residual set $\mathcal{R}_c$ in Equation (63) no later than $T_c/\sqrt{\theta}$, independently of the initial condition. Consequently, the position, trajectory, angular-rate, and velocity tracking errors are practically predefined-time stable, while the attitude errors remain inside their prescribed-performance boundaries.

Proof. For a scalar Lyapunov component V_ℓ , define

$$\mathcal{D}_\ell(V_\ell) = \frac{\pi}{\varphi_\ell \sqrt{m_\ell n_\ell T_\ell}} \left(m_\ell V_\ell^{1-\varphi_\ell/2} + n_\ell V_\ell^{1+\varphi_\ell/2} \right), \quad (41)$$

where $m_\ell > 0$, $n_\ell > 0$, $T_\ell > 0$, and $0 < \varphi_\ell < 1$ are the parameters assigned to the channel indexed by ℓ .

For a scalar channel using the regularized variable $V_{\ell,\epsilon} = \max\{V_\ell, \epsilon_\ell\}$, define its actual dissipation and maximum regularization loss as

$$\begin{aligned} \mathcal{D}_{\ell,\epsilon}(V_\ell) &= 2k_{\ell,\epsilon}^{\text{pt}} V_\ell, \\ \omega_{\ell,\epsilon} &= \sup_{0 \leq V_\ell \leq \epsilon_\ell} [\mathcal{D}_\ell(V_\ell) - \mathcal{D}_{\ell,\epsilon}(V_\ell)]_+, \end{aligned} \quad (42)$$

where $[a]_+ = \max\{a, 0\}$. For a component without a negative-power evaluation, set $\mathcal{D}_{\ell,\epsilon} = \mathcal{D}_\ell$ and $\omega_{\ell,\epsilon} = 0$. Because the regularized and ideal gains coincide for $V_\ell \geq \epsilon_\ell$, one has

$$-\mathcal{D}_{\ell,\epsilon}(V_\ell) \leq -\mathcal{D}_\ell(V_\ell) + \omega_{\ell,\epsilon}. \quad (43)$$

Let $\mathcal{I}_f = \{2, 3, 4\}$ identify PTDF1–PTDF3. For $i \in \mathcal{I}_f$, set $V_{fi} = e_{fi}^\top e_{fi}/2$. The filter equations in Section 4 give

$$\dot{V}_{fi} = -e_{fi}^\top K_{fi}^{\text{pt}} e_{fi} = - \sum_{j \in \mathcal{J}_{fi}} \mathcal{D}_{fi,j,\epsilon}(V_{fi,j}), \quad i \in \mathcal{I}_f, \quad (44)$$

where $V_{fi,j} = e_{fi,j}^2/2$, $\mathcal{J}_{f2} = \{\chi, \gamma\}$, $\mathcal{J}_{f3} = \{\mu, \alpha, \beta\}$, and $\mathcal{J}_{f4} = \{p, q, r\}$.

Before incorporating the PTDO terms into the composite analysis, the disturbance-estimation residual is derived from the coupled observer dynamics. Consider a scalar observer channel and abbreviate its state and disturbance estimation errors as $e_o = \tilde{x}_j$ and

$\delta_o = \tilde{d}_j$, respectively. Its error dynamics are $\dot{e}_o = \delta_o - k_o(e_o)e_o$ and $\dot{\delta}_o = \dot{d}_j - k_d k_o^2(e_o)e_o$, where $k_o(e_o) \geq \underline{k}_o > 0$. Select a sufficiently small constant $\sigma_o > 0$ and define

$$W_o = \frac{1}{2}\delta_o^2 + k_d \int_0^{e_o} k_o^2(s)s \, ds - \sigma_o e_o \delta_o.$$

The conditions $\sigma_o^2 < k_d \underline{k}_o^2$ and $k_d(\underline{k}_o - \sigma_o) > \sigma_o/2$ ensure that W_o is positive definite. Differentiating it along the two error equations and applying Young's inequality give

$$\begin{aligned} \dot{W}_o &= -k_d k_o^3(e_o)e_o^2 - \sigma_o \delta_o^2 + \sigma_o k_o(e_o)e_o \delta_o \\ &\quad + \sigma_o k_d k_o^2(e_o)e_o^2 + (\delta_o - \sigma_o e_o)\dot{d}_j \\ &\leq -\lambda_o(e_o^2 + \delta_o^2) + \sqrt{1 + \sigma_o^2} \bar{v}_j \sqrt{e_o^2 + \delta_o^2}, \end{aligned}$$

where $\lambda_o = \min\{\sigma_o/2, \underline{k}_o^2[k_d(\underline{k}_o - \sigma_o) - \sigma_o/2]\} > 0$. Hence, $\dot{W}_o < 0$ whenever $\sqrt{e_o^2 + \delta_o^2} > r_o$, where $r_o = \sqrt{1 + \sigma_o^2} \bar{v}_j / \lambda_o$. The joint estimation error is therefore bounded and ultimately enters this rate-dependent residual region. Applying the same argument componentwise gives a finite uniform bound $\|\tilde{d}_i\| \leq \tilde{d}_i$ for each vector observer, with its ultimate radius determined by the corresponding $\bar{v}_j$ and observer gains.

The trajectory, angular-rate, and velocity channels employ separate PTDOs. Introduce $\mathcal{I}_o = \{2, 4, V\}$ and $V_{oi} = \tilde{x}_i^\top \tilde{x}_i / 2$ for $i \in \mathcal{I}_o$. Using the first coupled observer-error relation given in Section 3.3,

$$\begin{aligned} \dot{V}_{oi} &= \tilde{x}_i^\top \tilde{d}_i - \tilde{x}_i^\top K_{oi} \tilde{x}_i \\ &\leq -\tilde{x}_i^\top K_{oi} \tilde{x}_i + \frac{\epsilon_{oi}}{2} \|\tilde{x}_i\|^2 + \frac{1}{2\epsilon_{oi}} \|\tilde{d}_i\|^2 \\ &\leq -\sum_{j \in \mathcal{J}_{oi}} \mathcal{D}_{oi,j,\epsilon}(V_{oi,j}) - c_{oi} \|\tilde{x}_i\|^2 + \Omega_{oi}, \quad i \in \mathcal{I}_o, \end{aligned} \quad (45)$$

where $\epsilon_{oi} > 0$ is a Young-inequality parameter, and $\mathcal{J}_{o2} = \{\chi, \gamma\}$, $\mathcal{J}_{o4} = \{p, q, r\}$, and $\mathcal{J}_{oV} = \{V\}$. The remaining linear damping margin and the observer residual are defined as $c_{oi} = \lambda_{\min}(K_{l,i}) - \epsilon_{oi}/2 > 0$ and $\Omega_{oi} = \tilde{d}_i^2 / (2\epsilon_{oi})$, respectively. The latter is bounded under Assumption 1 and the joint observer-error analysis above. Consequently, the total observer Lyapunov function and residual are $V_o = V_{o2} + V_{o4} + V_{oV}$ and $\Omega_o = \Omega_{o2} + \Omega_{o4} + \Omega_{oV}$, respectively.

Since $x_2 = x_2^c + e_2 + e_{f2} + b_\gamma \eta_{as,\gamma}$, substituting Equation (22) into Equation (21) gives

$$\dot{e}_1 = -K_1^{\text{pt}} e_1 + g_1 e_2 + g_1 e_{f2} + g_1 b_\gamma \eta_{as,\gamma}. \quad (46)$$

Choose $V_1 = e_1^\top e_1 / 2$, and let $\|g_1\| \leq \bar{g}_1$. For arbitrary $\epsilon_{12} > 0$, $\epsilon_{1f} > 0$, and $\epsilon_{1a} > 0$, the Cauchy–Schwarz and Young inequalities yield

$$\begin{aligned} \dot{V}_1 &= -e_1^\top K_1^{\text{pt}} e_1 + e_1^\top g_1 e_2 + e_1^\top g_1 e_{f2} + e_1^\top g_1 b_\gamma \eta_{as,\gamma} \\ &\leq -\sum_{j \in \{y,z\}} \mathcal{D}_{1j,\epsilon}(V_{1j}) + \bar{g}_1 \|e_1\| \|e_2\| + \bar{g}_1 \|e_1\| \|e_{f2}\| + \bar{g}_1 \|e_1\| \|\eta_{as,\gamma}\| \\ &\leq -\sum_{j \in \{y,z\}} \mathcal{D}_{1j,\epsilon}(V_{1j}) + \frac{\epsilon_{12} + \epsilon_{1f} + \epsilon_{1a}}{2} \|e_1\|^2 + \frac{\bar{g}_1^2}{2\epsilon_{12}} \|e_2\|^2 + \frac{\bar{g}_1^2}{2\epsilon_{1f}} \|e_{f2}\|^2 + \frac{\bar{g}_1^2}{2\epsilon_{1a}} \eta_{as,\gamma}^2. \end{aligned} \quad (47)$$

Let $\Delta u_2 = u_2 - u_2^c$ be the realization error of the spoiler and trailing-edge-flap commands. Differentiating $e_2 = x_2 - x_{2,as}^c$ and using Equations (25) and (26) result in

$$\dot{e}_2 = -K_2^{\text{pt}} e_2 + \tilde{d}_2 + g_2 \Delta u_2 - b_\gamma \dot{\eta}_{as,\gamma}. \quad (48)$$

With $V_2 = \mathbf{e}_2^\top \mathbf{e}_2 / 2$, $\|\mathbf{g}_2\| \leq \bar{g}_2$, and positive constants ϵ_{2d} , ϵ_{2u} , and ϵ_{2a} , one has

$$\begin{aligned} \dot{V}_2 &= -\mathbf{e}_2^\top \mathbf{K}_2^{\text{pt}} \mathbf{e}_2 + \mathbf{e}_2^\top \tilde{\mathbf{d}}_2 + \mathbf{e}_2^\top \mathbf{g}_2 \Delta \mathbf{u}_2 - \mathbf{e}_2^\top \mathbf{b}_\gamma \dot{\eta}_{\text{as},\gamma} \\ &\leq - \sum_{j \in \{\chi, \gamma\}} \mathcal{D}_{2j,\epsilon}(V_{2j}) + \|\mathbf{e}_2\| \|\tilde{\mathbf{d}}_2\| + \bar{g}_2 \|\mathbf{e}_2\| \|\Delta \mathbf{u}_2\| + \|\mathbf{e}_2\| |\dot{\eta}_{\text{as},\gamma}| \\ &\leq - \sum_{j \in \{\chi, \gamma\}} \mathcal{D}_{2j,\epsilon}(V_{2j}) + \frac{\epsilon_{2d} + \epsilon_{2u} + \epsilon_{2a}}{2} \|\mathbf{e}_2\|^2 + \frac{\|\tilde{\mathbf{d}}_2\|^2}{2\epsilon_{2d}} + \frac{\bar{g}_2^2 \|\Delta \mathbf{u}_2\|^2}{2\epsilon_{2u}} + \frac{|\dot{\eta}_{\text{as},\gamma}|^2}{2\epsilon_{2a}}. \end{aligned} \quad (49)$$

The relation $\mathbf{x}_4 = \mathbf{x}_4^c + \mathbf{e}_4 + \mathbf{e}_{f4}$, together with Equations (29) and (31), gives $\dot{\mathbf{e}}_3 = -\mathbf{v}_3 + \mathbf{H}_3 \mathbf{g}_3 (\mathbf{e}_4 + \mathbf{e}_{f4})$. Select $V_3 = \mathbf{e}_3^\top \mathbf{e}_3 / 2$. Over the feasible PPC domain, let $\|\mathbf{H}_3 \mathbf{g}_3\| \leq \bar{h}_3$. For $\epsilon_{34} > 0$ and $\epsilon_{3f} > 0$,

$$\begin{aligned} \dot{V}_3 &= -\mathbf{e}_3^\top \mathbf{K}_3^{\text{pt}} \mathbf{e}_3 + \mathbf{e}_3^\top \mathbf{H}_3 \mathbf{g}_3 \mathbf{e}_4 + \mathbf{e}_3^\top \mathbf{H}_3 \mathbf{g}_3 \mathbf{e}_{f4} \\ &\leq - \sum_{j \in \{\mu, \alpha, \beta\}} \mathcal{D}_{3j,\epsilon}(V_{3j}) + \bar{h}_3 \|\mathbf{e}_3\| \|\mathbf{e}_4\| + \bar{h}_3 \|\mathbf{e}_3\| \|\mathbf{e}_{f4}\| \\ &\leq - \sum_{j \in \{\mu, \alpha, \beta\}} \mathcal{D}_{3j,\epsilon}(V_{3j}) + \frac{\epsilon_{34} + \epsilon_{3f}}{2} \|\mathbf{e}_3\|^2 + \frac{\bar{h}_3^2}{2\epsilon_{34}} \|\mathbf{e}_4\|^2 + \frac{\bar{h}_3^2}{2\epsilon_{3f}} \|\mathbf{e}_{f4}\|^2. \end{aligned} \quad (50)$$

Boundedness of $\mathbf{e}_3$ guarantees $|\zeta_{3j}| < 1$, and therefore ensures that the attitude tracking errors remain within their prescribed-performance boundaries.

Define $\Delta \mathbf{u}_4 = \mathbf{u}_4 - \mathbf{u}_4^c$. Substitution of Equation (36) into Equation (33) gives $\dot{\mathbf{e}}_4 = -\mathbf{K}_4^{\text{pt}} \mathbf{e}_4 + \tilde{\mathbf{d}}_4 + \mathbf{g}_4 \Delta \mathbf{u}_4$. For $V_4 = \mathbf{e}_4^\top \mathbf{e}_4 / 2$, $\|\mathbf{g}_4\| \leq \bar{g}_4$, $\epsilon_{4d} > 0$, and $\epsilon_{4u} > 0$,

$$\begin{aligned} \dot{V}_4 &= -\mathbf{e}_4^\top \mathbf{K}_4^{\text{pt}} \mathbf{e}_4 + \mathbf{e}_4^\top \tilde{\mathbf{d}}_4 + \mathbf{e}_4^\top \mathbf{g}_4 \Delta \mathbf{u}_4 \\ &\leq - \sum_{j \in \{p, q, r\}} \mathcal{D}_{4j,\epsilon}(V_{4j}) + \|\mathbf{e}_4\| \|\tilde{\mathbf{d}}_4\| + \bar{g}_4 \|\mathbf{e}_4\| \|\Delta \mathbf{u}_4\| \\ &\leq - \sum_{j \in \{p, q, r\}} \mathcal{D}_{4j,\epsilon}(V_{4j}) + \frac{\epsilon_{4d} + \epsilon_{4u}}{2} \|\mathbf{e}_4\|^2 + \frac{\|\tilde{\mathbf{d}}_4\|^2}{2\epsilon_{4d}} + \frac{\bar{g}_4^2 \|\Delta \mathbf{u}_4\|^2}{2\epsilon_{4u}}. \end{aligned} \quad (51)$$

Let $\Delta T = T - T^c$. Equations (37) and (40) yield $\dot{e}_V = -k_V^{\text{pt}} e_V + \tilde{d}_V + g_V \Delta T$. Select $V_V = e_V^2 / 2$, and let $|g_V| \leq \bar{g}_V$. For $\epsilon_{Vd} > 0$ and $\epsilon_{VT} > 0$,

$$\begin{aligned} \dot{V}_V &= -k_V^{\text{pt}} e_V^2 + e_V \tilde{d}_V + e_V g_V \Delta T \\ &\leq -\mathcal{D}_{V,\epsilon}(V_V) + |e_V| |\tilde{d}_V| + \bar{g}_V |e_V| |\Delta T| \\ &\leq -\mathcal{D}_{V,\epsilon}(V_V) + \frac{\epsilon_{Vd} + \epsilon_{VT}}{2} e_V^2 + \frac{\tilde{d}_V^2}{2\epsilon_{Vd}} + \frac{\bar{g}_V^2 \Delta T^2}{2\epsilon_{VT}}. \end{aligned} \quad (52)$$

Choose $V_{\text{as}} = \boldsymbol{\eta}_{\text{as}}^\top \boldsymbol{\eta}_{\text{as}} / 2$. The continuous realization of the robust term in Equation (19) introduces $r_j = |\eta_{\text{as},j}| - \eta_{\text{as},j} \tanh(\eta_{\text{as},j} / b_j)$, where $b_j > 0$. Lemma 3 gives $0 \leq r_j \leq 0.2785 b_j$, and hence $\sum_j k_3 r_j \leq 0.2785 k_3 \sum_j b_j \triangleq \Omega_{\text{sm}}$. Applying Young's inequality to $\boldsymbol{\eta}_{\text{as}}^\top \mathbf{G}_{\text{as}} \Delta \mathbf{u}$ then gives

$$\dot{V}_{\text{as}} \leq - \sum_{j \in \mathcal{J}_{\text{as}}} \mathcal{D}_{\text{as},j}(V_{\text{as},j}) + c_{\text{as}} \|\boldsymbol{\eta}_{\text{as}}\|^2 + \Omega_{\text{as}}, \quad (53)$$

where $c_{\text{as}} > 0$ is the coefficient introduced by the bounded saturation input, and Ω_{as} contains Ω_{sm} and the remaining bounded saturation residuals.

The composite Lyapunov function is selected as

$$V_c = V_1 + V_2 + V_3 + V_4 + V_V + V_{\text{as}} + \sum_{i \in \mathcal{I}_f} V_{fi} + \sum_{i \in \mathcal{I}_o} V_{oi}. \quad (54)$$

Define the augmented error vector explicitly as

$$\zeta_c = \text{col} (e_1, e_2, e_3, e_4, e_V, \eta_{as}, e_{f2}, e_{f3}, e_{f4}, \tilde{x}_2, \tilde{x}_4, \tilde{x}_V),$$

so that $V_c = \zeta_c^\top \zeta_c / 2$.

To specify every scalar component in Equation (54), define

$$\begin{aligned} \mathcal{L} = & \{(i, j) : i \in \{1, 2, 3, 4\}, j \in \mathcal{J}_i\} \cup \{V, as\} \\ & \cup \{(fi, j) : i \in \{2, 3, 4\}, j \in \mathcal{J}_i\} \cup \{(oi, j) : i \in \{2, 4\}, j \in \mathcal{J}_i\} \cup \{oV\}, \end{aligned} \quad (55)$$

where $\mathcal{J}_1 = \{y, z\}$, $\mathcal{J}_2 = \{\chi, \gamma\}$, $\mathcal{J}_3 = \{\mu, \alpha, \beta\}$, and $\mathcal{J}_4 = \{p, q, r\}$. Thus, V_ℓ denotes the scalar Lyapunov component associated with $\ell \in \mathcal{L}$; for example, $V_{(1,y)} = e_y^2 / 2$, $V_{(3,\alpha)} = e_{3\alpha}^2 / 2$, and $V_{(o4,r)} = \tilde{x}_{4r}^2 / 2$.

Combining Equations (44)–(53) gives the expanded composite bound

$$\begin{aligned} \dot{V}_c \leq & - \sum_{\ell \in \mathcal{L}} \mathcal{D}_{\ell, \epsilon}(V_\ell) + \bar{\Omega}_c \\ & + \frac{\epsilon_{12} + \epsilon_{1f} + \epsilon_{1a}}{2} \|e_1\|^2 \\ & + \left(\frac{\epsilon_{2d} + \epsilon_{2u} + \epsilon_{2a}}{2} + \frac{\bar{g}_1^2}{2\epsilon_{12}} \right) \|e_2\|^2 \\ & + \frac{\epsilon_{34} + \epsilon_{3f}}{2} \|e_3\|^2 \\ & + \left(\frac{\epsilon_{4d} + \epsilon_{4u}}{2} + \frac{\bar{h}_3^2}{2\epsilon_{34}} \right) \|e_4\|^2 \\ & + \frac{\epsilon_{Vd} + \epsilon_{VT}}{2} e_V^2 + \frac{\bar{g}_1^2}{2\epsilon_{1f}} \|e_{f2}\|^2 \\ & + \frac{\bar{h}_3^2}{2\epsilon_{3f}} \|e_{f4}\|^2 + \left(c_{as} + \frac{\bar{g}_1^2}{2\epsilon_{1a}} \right) \|\eta_{as}\|^2, \end{aligned} \quad (56)$$

where $\bar{\Omega}_c$ collects the bounded observer, actuator, anti-saturation, and reference-related residual terms not assigned to the quadratic coefficients. The total positive quadratic coefficients are therefore defined as

$$\begin{aligned} q_{1j} &= \frac{\epsilon_{12} + \epsilon_{1f} + \epsilon_{1a}}{2}, \\ q_{2j} &= \frac{\epsilon_{2d} + \epsilon_{2u} + \epsilon_{2a}}{2} + \frac{\bar{g}_1^2}{2\epsilon_{12}}, \\ q_{3j} &= \frac{\epsilon_{34} + \epsilon_{3f}}{2}, \\ q_{4j} &= \frac{\epsilon_{4d} + \epsilon_{4u}}{2} + \frac{\bar{h}_3^2}{2\epsilon_{34}}, \\ q_V &= \frac{\epsilon_{Vd} + \epsilon_{VT}}{2}, \\ q_{f2,j} &= \frac{\bar{g}_1^2}{2\epsilon_{1f}}, \quad q_{f4,j} = \frac{\bar{h}_3^2}{2\epsilon_{3f}}, \\ q_{as} &= c_{as} + \frac{\bar{g}_1^2}{2\epsilon_{1a}}. \end{aligned} \quad (57)$$

Let $\mathcal{L}_q \subset \mathcal{L}$ contain the scalar components associated with the coefficients in Equation (57). By Equation (43), define $\Omega_\epsilon = \sum_{\ell \in \mathcal{L}} \omega_{\ell, \epsilon}$ and $\bar{\Omega}_\epsilon^\epsilon = \bar{\Omega}_c + \Omega_\epsilon$. Equation (56) can then be written as

$$\dot{V}_c \leq - \sum_{\ell \in \mathcal{L}} \mathcal{D}_\ell(V_\ell) + 2 \sum_{\ell \in \mathcal{L}_q} q_\ell V_\ell + \bar{\Omega}_c^\epsilon.$$

For every $\ell \in \mathcal{L}$, the arithmetic–geometric mean inequality gives $\mathcal{D}_\ell(V_\ell) \geq 2\pi V_\ell / (\varphi_\ell T_\ell)$. Consequently, the explicit channel-wise sufficient conditions are

$$\frac{\pi}{\varphi_\ell T_\ell} > q_\ell, \quad \ell \in \mathcal{L}_q. \quad (58)$$

Under Equation (58), define the retained dissipation margins as

$$\rho_\ell = \begin{cases} 1 - \frac{\varphi_\ell T_\ell q_\ell}{\pi}, & \ell \in \mathcal{L}_q, \\ 1, & \ell \in \mathcal{L} \setminus \mathcal{L}_q, \end{cases} \quad 0 < \rho_\ell \leq 1. \quad (59)$$

The preceding lower bound implies $-\mathcal{D}_\ell(V_\ell) + 2q_\ell V_\ell \leq -\rho_\ell \mathcal{D}_\ell(V_\ell)$ for every $\ell \in \mathcal{L}_q$. Therefore, summing the component-wise inequalities yields

$$\dot{V}_c \leq - \sum_{\ell \in \mathcal{L}} \rho_\ell \mathcal{D}_\ell(V_\ell) + \bar{\Omega}_c^\epsilon. \quad (60)$$

The implementation adopts a common exponent φ for all predefined-time channels. Let $N_{\mathcal{L}} = |\mathcal{L}| = 26$, and define

$$\begin{aligned} \kappa_c^- &= \min_{\ell \in \mathcal{L}} \left\{ \frac{\pi \rho_\ell}{\varphi T_\ell} \sqrt{\frac{m_\ell}{n_\ell}} \right\}, \\ \kappa_c^+ &= N_{\mathcal{L}}^{-\varphi/2} \min_{\ell \in \mathcal{L}} \left\{ \frac{\pi \rho_\ell}{\varphi T_\ell} \sqrt{\frac{n_\ell}{m_\ell}} \right\}. \end{aligned} \quad (61)$$

Here, the superscripts $(-)$ and $(+)$ correspond to the powers $1 - \varphi/2$ and $1 + \varphi/2$, respectively. By Lemma 2,

$$\sum_{\ell \in \mathcal{L}} \rho_\ell \mathcal{D}_\ell(V_\ell) \geq \kappa_c^- V_c^{1-\varphi/2} + \kappa_c^+ V_c^{1+\varphi/2}.$$

Hence,

$$\dot{V}_c \leq -\kappa_c^- V_c^{1-\varphi/2} - \kappa_c^+ V_c^{1+\varphi/2} + \bar{\Omega}_c^\epsilon. \quad (62)$$

For any $\vartheta \in (0, 1)$, define

$$\bar{V}_c = \min \left\{ \left[\frac{\bar{\Omega}_c^\epsilon}{(1-\vartheta)\kappa_c^-} \right]^{\frac{2}{2-\varphi}}, \left[\frac{\bar{\Omega}_c^\epsilon}{(1-\vartheta)\kappa_c^+} \right]^{\frac{2}{2+\varphi}} \right\}.$$

The residual set of the complete cascade is

$$\mathcal{R}_c = \{\zeta_c : V_c(\zeta_c) \leq \bar{V}_c\}. \quad (63)$$

Because $V_c = \zeta_c^\top \zeta_c / 2$, the corresponding aggregate error bound is $\|\zeta_c\| \leq \sqrt{2\bar{V}_c}$. Finally, define the equivalent aggregate predefined-time parameter as $T_c = \pi / (\varphi \sqrt{\kappa_c^- \kappa_c^+})$. Equation (62) has the form of Lemma 1 with $m_c = \kappa_c^-$, $n_c = \kappa_c^+$, and T_c as defined above. Therefore, ζ_c reaches $\mathcal{R}_c$ within $T_s \leq T_c / \sqrt{\vartheta}$. The coefficients κ_c^- , κ_c^+ , and T_c depend on the

selected channel parameters, the retained dissipation margins, and $N_{\mathcal{L}}$, but not on $V_c(0)$. Thus, the time bound is independent of the initial condition. When $\bar{\Omega}_c^\varepsilon = 0$, the residual set reduces to the origin. In addition, boundedness of ε_3 guarantees that the bank-angle, angle-of-attack, and sideslip-angle errors remain inside their prescribed-performance boundaries. $\square$

6. Simulations

To validate the proposed control scheme, numerical simulations were conducted using a nonlinear six-DOF fixed-wing UAV model in the carrier landing phase. First, the predefined-time convergence property was tested under different initial approach errors. Second, the benefit of the integrated direct lift and side-force architecture was examined by comparing it with reduced direct-force configurations. Third, the complete Predefined-Time Direct Lift/Side-Force-Integrated Approach Landing (PTDIAL) controller was compared with several baseline controllers to evaluate tracking accuracy, attitude response, actuator behavior, and disturbance-rejection capability. Finally, Monte Carlo simulations were performed to assess robustness under randomized initial errors, airwake intensity, sea state scale, and gust parameters.

6.1. Simulation Setup

The initial longitudinal position of the UAV was set as $x_0 = -1472$ m, where the nominal ITP was located at $x = 0$ at the initial instant. Unless otherwise specified, the initial vertical and lateral offsets were $z_0 = 8$ m and $y_0 = 8$ m, respectively. For the predefined-time convergence test, several different initial offsets were additionally used to verify the convergence behavior under different approach errors.

The UAV was initialized at the trimmed approach condition. The trim airspeed was $V_0 = 70$ m/s, the trim angle of attack was $\alpha_0 = 8^\circ$, and the trim sideslip angle, bank-angle, and angular rates were set to zero. The flight-path angle and heading angle were determined by the nominal glide-slope geometry and ship motion. The elevator, thrust, and flap trim values were obtained from the nonlinear trim calculation and then kept as the initial actuator commands.

The principal controller and PTDO parameters used in all simulation scenarios are summarized in Tables A5 and A6, respectively, while the actuator settings are given in Table A3.

Three simulation scenarios were considered, as summarized in Table 1. Scenario 1 was used to verify the predefined-time convergence property. Scenario 2 was used to evaluate the effectiveness of the integrated direct lift/side-force architecture. Scenario 3 was used for controller comparison and Monte Carlo verification under combined disturbances. The carrier airwake was modeled according to the MIL-F-8785C standard [35], and the Ideal Touch Point (ITP) motion under different sea states was generated according to the method in the reference [9]. Scenario 2 included a lateral gust with an amplitude of 5 m/s, injected at $t = 15.0$ s. In Scenario 3, a lateral gust with an amplitude of 5 m/s was injected at $t = 15.0$ s, and a vertical gust with the same amplitude was injected at $t = 20.0$ s.

The comparison algorithms are summarized in Table 2. Scenario 2 varied the direct-force configuration to isolate its contribution, whereas Scenario 3 retained the integrated direct lift/side-force architecture to compare the control laws, disturbance observers, and anti-saturation mechanism. Within each scenario, all methods used the same nonlinear model, flight condition, reference, actuator constraints, and environmental realization. PTDIAL-NA retained the parameters of PTDIAL with only the flap anti-saturation mechanism disabled; FxTDI and NDI, constructed following [3] and [39], respectively, were indepen-

dently tuned under the same nominal scenario. The selected parameters remained fixed, and identical randomized cases were used in the Monte Carlo comparison.

Table 1. Simulation scenarios.

Scenario	Purpose	Initial Offset	Environment
1	Predefined-time convergence	Multiple z_0, y_0 cases	Light airwake, normal sea state, no gust
2	Direct-force architecture validation	$z_0 = y_0 = 8$ m	Moderate airwake, normal sea state, lateral gust
3	Algorithm comparison	$z_0 = y_0 = 8$ m	Heavy airwake, high sea state, lateral and vertical gusts

Table 2. Comparison algorithms.

Scenario	Algorithm	Description
2/3	PTDIAL	Proposed method
2	PTDL	Direct lift only, without direct side force
2	PT	No direct-force control architecture
3	PTDIAL-NA	PTDIAL without the anti-saturation mechanism
3	FxTDI	Fixed-time direct-force controller with fixed-time disturbance observation
3	NDI	Nonlinear dynamic inversion with conventional disturbance observation

6.2. Predefined-Time Convergence Verification

To evaluate predefined-time convergence, the initial error in each position channel was set to $e_i(0) \in \{-8, -6, -4, -2, 2, 4, 6, 8\}$ m, $i \in \{y, z\}$, while the remaining states were initialized near the trim condition; the eight colored curves in each panel correspond, respectively, to these initial offsets. The position-loop design times were $T_y = 7$ s and $T_z = 11$ s, with convergence recorded when the corresponding error entered and remained within its bounded steady-state neighborhood.

Figure 5 shows that all lateral and vertical errors converge within T_y and T_z , respectively, with little variation across the tested initial values. The vertical transient caused by the subsequent ITP motion is also rejected. Scenario 1 isolates the effect of the initial conditions, whereas Sections 6.4 and 6.5 examine combined and randomized disturbances; under the conditions of Theorem 1, the convergence-time bound remains independent of the initial state.

To further illustrate the approach process, Figure 6 presents the three-dimensional landing trajectories under four extreme initial conditions. The UAV starts from different vertical and lateral offsets and finally approaches the neighborhood of the ITP. This result further verifies the effectiveness of the predefined-time guidance and the integrated direct lift/side-force control architecture.

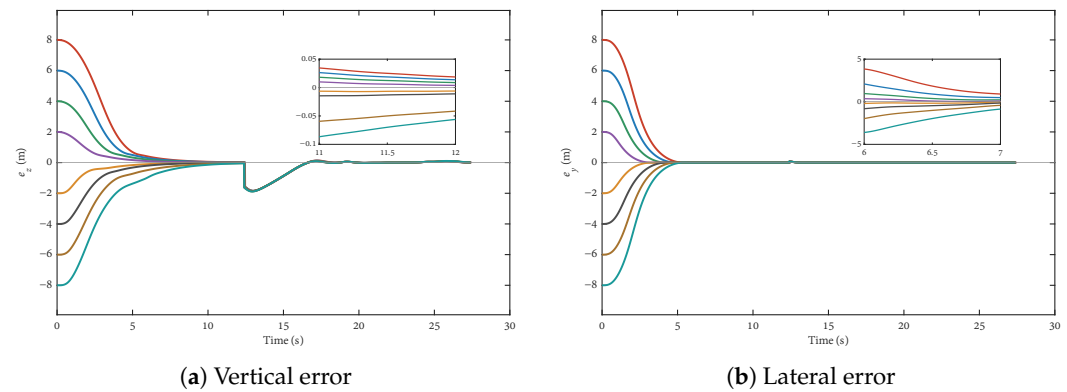


Figure 5. Position-error responses under different initial offsets.

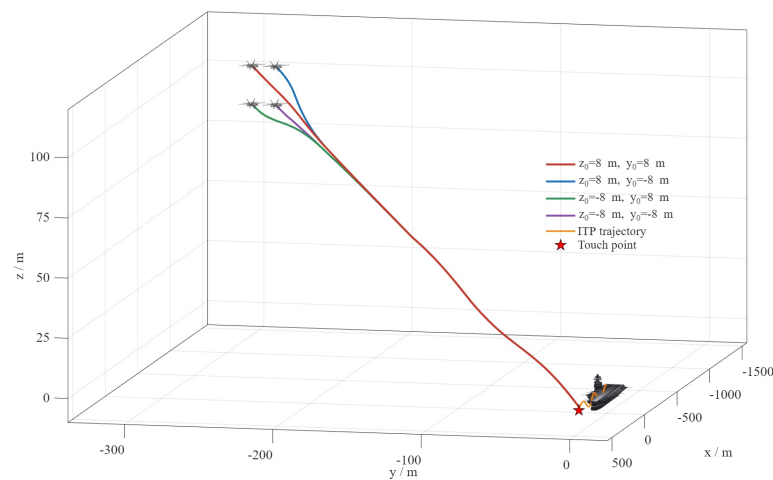


Figure 6. Three-dimensional landing trajectories.

6.3. Direct-Force Architecture Validation

The effectiveness of the integrated direct-force architecture was evaluated by comparing PTDIAL with two reduced configurations, namely, PTDL and PT. PTDIAL used both direct lift and direct side force, PTDL retained only the direct-lift channel, and PT removed both direct-force channels. This comparison was used to isolate the contribution of direct lift and direct side force to carrier landing control.

Figure 7 shows the position tracking results of the three control architectures. Compared with PTDL and PT, PTDIAL achieves faster and smoother lateral error convergence. This result indicates that the direct side-force channel can effectively reduce the dependence on bank-angle-induced lateral motion and improve lateral tracking performance. In the vertical channel, the direct-lift channel helps reduce the coupling between vertical motion and pitch attitude, leading to improved vertical tracking behavior compared with the non-direct-force configuration.

Figure 8 presents the attitude responses. In the bank-angle channel, PTDIAL keeps μ almost unchanged, since the spoiler-generated side force allows the UAV to correct the lateral error without relying on bank-angle/sideslip-mediated lateral correction. By contrast, PTDL and PT require evident bank-angle variations. In the angle-of-attack channel, PT shows large oscillations, whereas PTDIAL and PTDL remain close to the trim value due to the use of direct lift. In the sideslip channel, PTDIAL maintains β near zero with only small transient fluctuations, which are mainly related to the residual coupling introduced by spoiler-based side-force generation. The other two configurations exhibit more persistent sideslip oscillations.

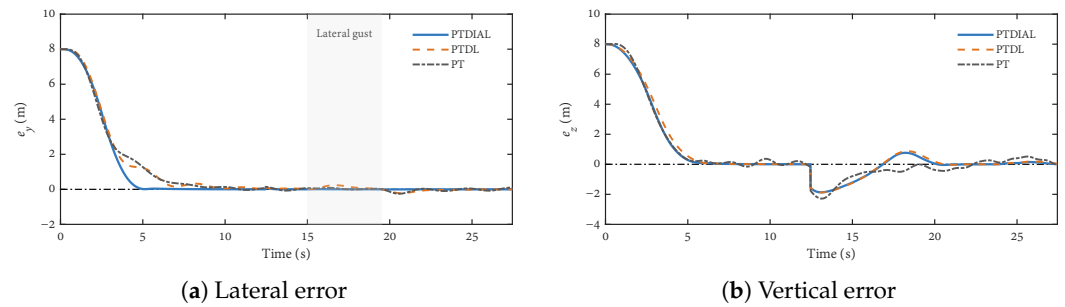


Figure 7. Position tracking results of different direct-force architectures.

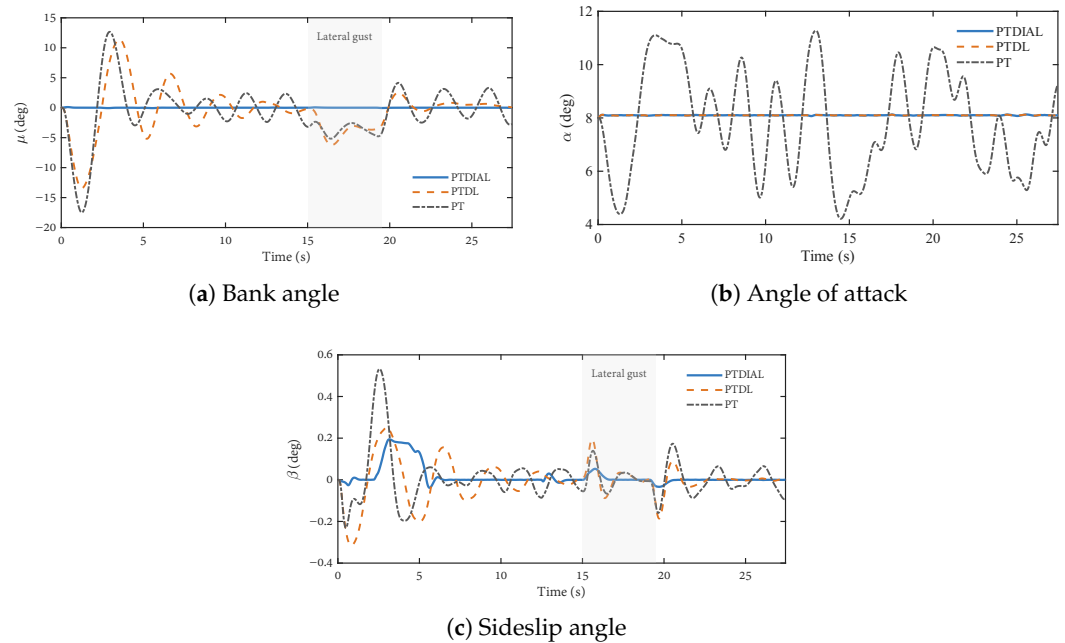


Figure 8. Attitude responses of different direct-force architectures.

Figure 9 shows the actuator responses. PTDIAL makes use of both the flap and spoiler channels to generate direct vertical and lateral forces. Although this introduces additional direct-force actuator activity, it reduces the demand on attitude maneuvering and improves the coordination of the landing control process. Overall, the results demonstrate the effectiveness of the integrated direct lift/side-force architecture under large initial position deviations and lateral disturbance.

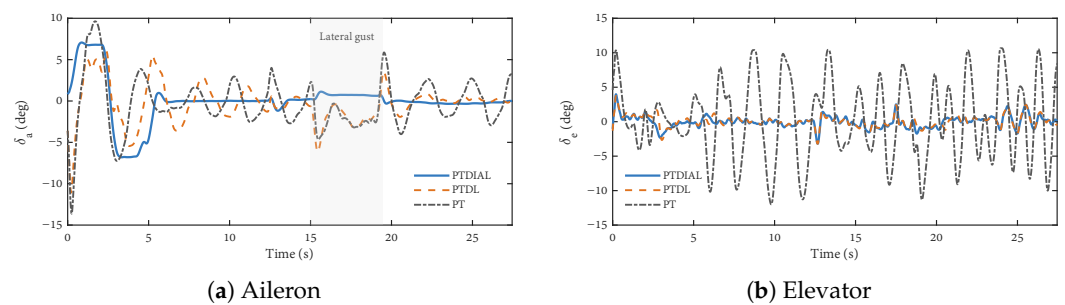


Figure 9. Cont.

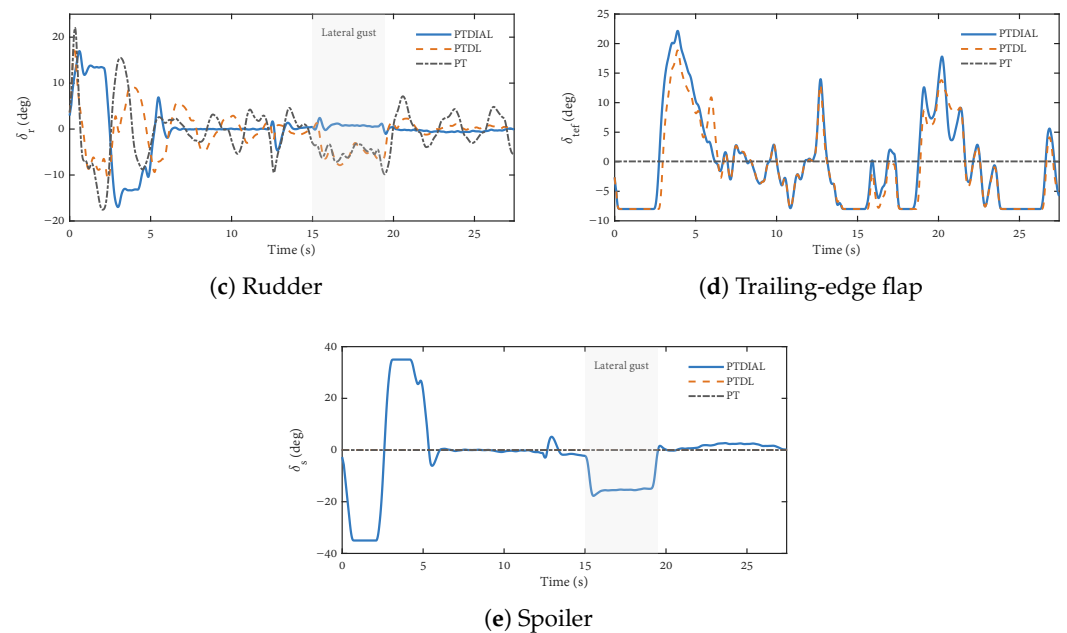


Figure 9. Actuator responses of different direct-force architectures.

6.4. Controller Comparison Under Combined Disturbances

Scenario 3 was used to evaluate the closed-loop performance of different controllers under combined ITP motion, carrier airwake, and gust disturbances. The compared controllers are those listed in Table 2.

Before presenting the closed-loop responses, the disturbance-observer performance is examined. The proposed predefined-time disturbance observer, denoted as PTDO, was compared with FxTDO and NDO in six channels, including heading, flight-path, roll rate, pitch rate, yaw rate, and velocity disturbances.

Figures 10 and 11 present the disturbance-observer comparison. For each disturbance channel, the estimated disturbance and the corresponding estimation error are shown together to provide a direct comparison of tracking accuracy and residual error. In the heading, roll rate, and yaw rate channels, PTDO provides faster and more accurate tracking of the disturbance variation caused by the lateral gust. In the flight-path, pitch rate, and velocity channels, PTDO also maintains smaller estimation errors during the vertical gust response. Compared with FxTDO, PTDO reduces the phase lag and improves the transient tracking accuracy. Compared with NDO, PTDO captures the disturbance variations more effectively, whereas NDO tends to produce smoother but delayed estimates. These results indicate that the proposed predefined-time observer can provide more accurate disturbance information for feedforward compensation and closed-loop control.

The closed-loop responses under Scenario 3 were further organized according to the control architecture, as shown in Figures 12–14. Figure 12 presents the position-loop-related responses, including the lateral and vertical position errors, the trajectory variables, and the direct-force actuators. All controllers reduce the initial position errors, while PTDLAL gives a smoother response under the combined deck motion and gust disturbances. Since the four methods use the same integrated direct lift/side-force architecture, the differences in this scenario mainly reflect the effects of the observer, predefined-time control law, and anti-saturation mechanism.

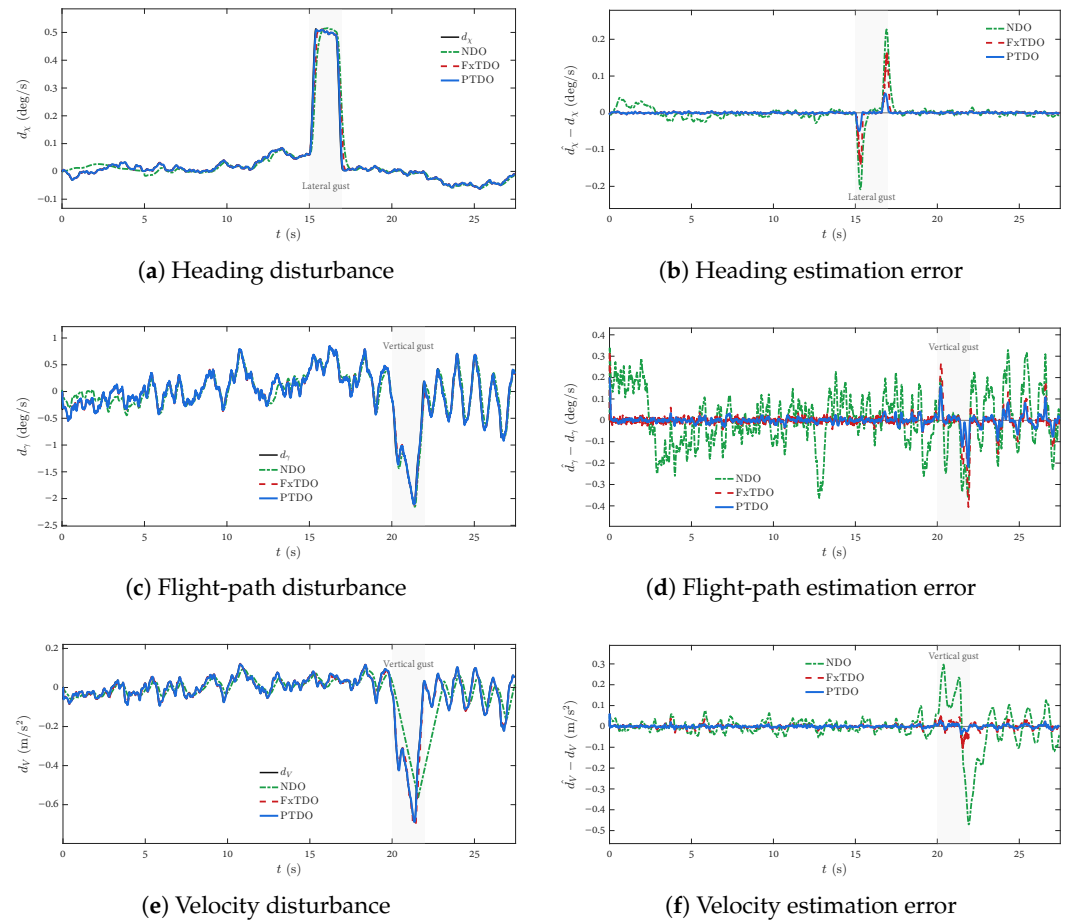


Figure 10. Disturbance estimation and estimation-error comparison in the heading, flight-path, and velocity channels.

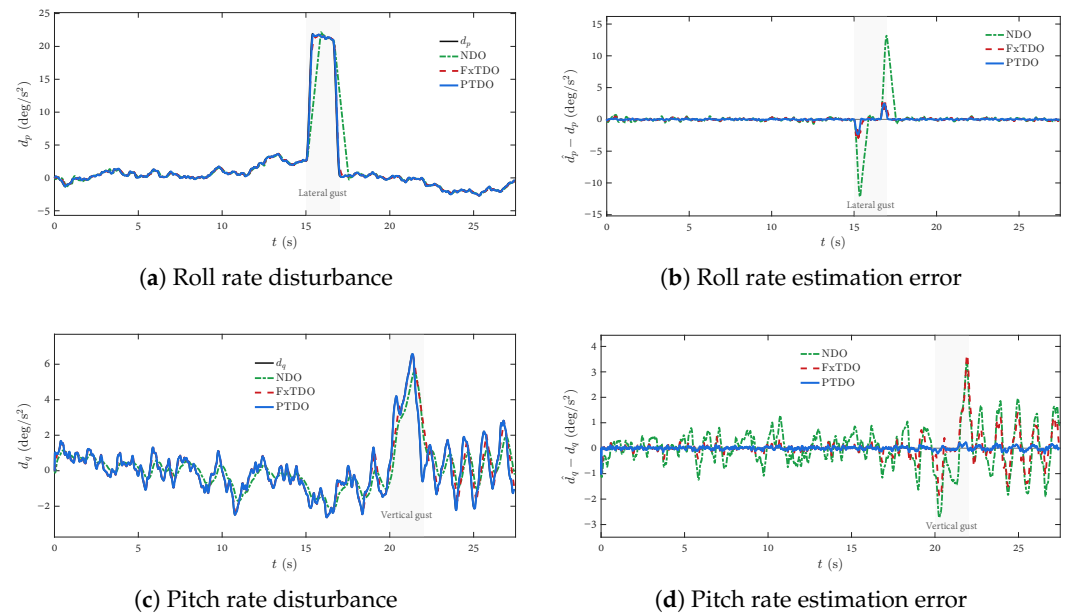


Figure 11. Cont.

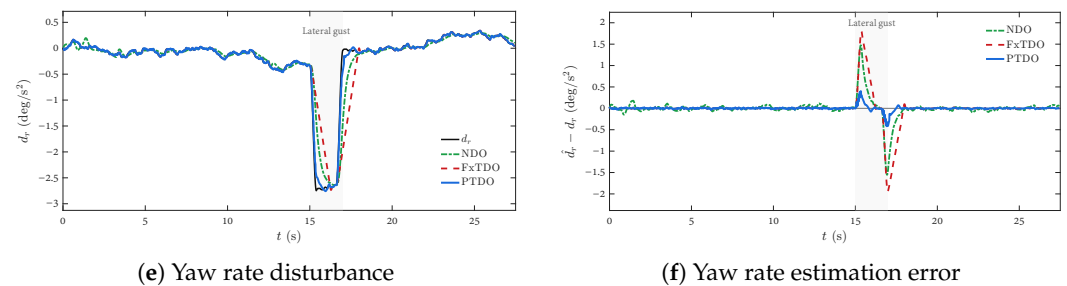


Figure 11. Disturbance estimation and estimation-error comparison in the angular rate channels.

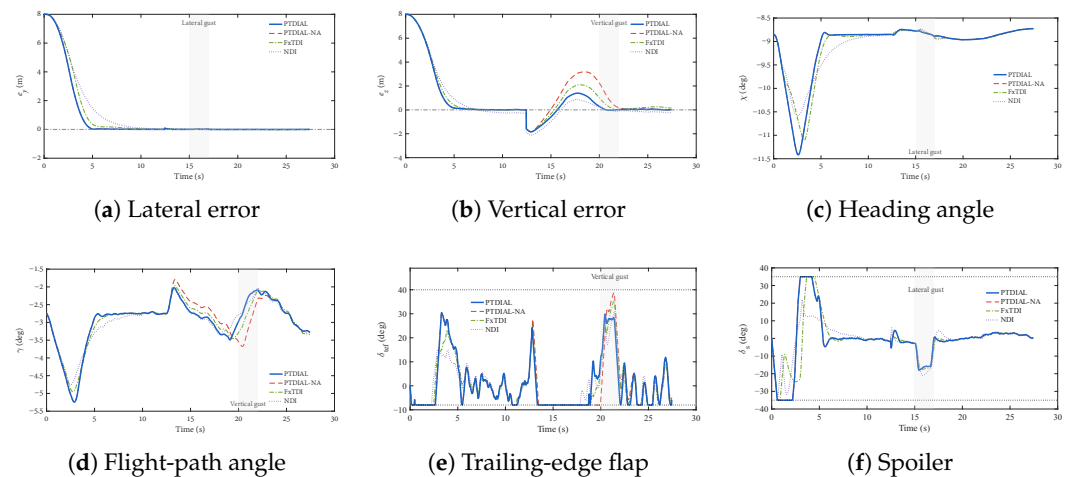


Figure 12. Position-control-related responses.

Figure 13 shows the attitude-loop responses and the conventional aerodynamic control surfaces. Owing to the use of direct-force control, the attitude variables are well regulated for all four methods, especially in the bank-angle and sideslip channels. During gust injection, PTDIAL and PTDIAL-NA exhibit smaller fluctuations in several channels, which is consistent with the improved disturbance-estimation accuracy shown above. The attitude responses also show that the proposed controller maintains the prescribed performance bounds while preserving small attitude deviations.

The velocity-loop responses are shown in Figure 14. The airspeed is maintained near its reference value under combined disturbances, and the thrust response remains bounded. Together with the actuator responses in Figure 12, the results indicate that the predefined-time requirement inevitably leads to relatively large transient commands, especially in the direct-lift channel. Without anti-saturation compensation, PTDIAL-NA produces a longer flap saturation interval, which further affects the vertical error, flight-path angle, angle-of-attack, pitch rate, and elevator response. By contrast, the proposed PTDIAL shortens the flap saturation duration and alleviates the coupled longitudinal response. These results indicate that the anti-saturation mechanism is not only used to limit actuator commands but also helps maintain the transient quality of the predefined-time closed-loop system under actuator constraints.

Overall, the comparison demonstrates that PTDIAL achieves a favorable balance among predefined-time convergence, disturbance rejection, attitude regulation, and actuator feasibility. Because the compared controllers share the same direct-force architecture, the improvement mainly comes from the combined action of the predefined-time prescribed performance controller, the predefined-time disturbance observer, and the flap anti-saturation design.

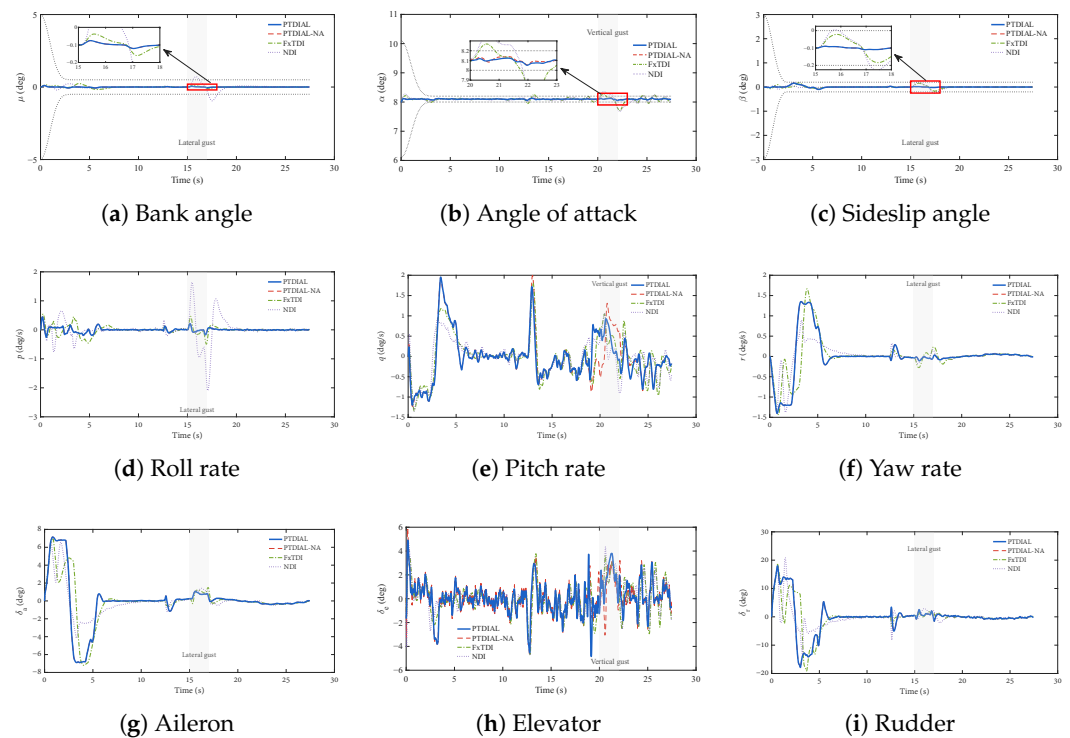


Figure 13. Attitude-control-related responses.

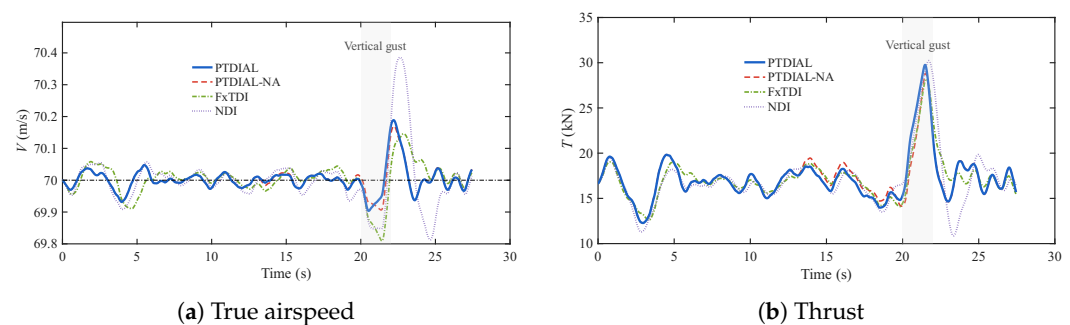


Figure 14. Velocity-control-related responses.

6.5. Monte Carlo Verification

To further evaluate robustness against uncertain landing conditions, Monte Carlo simulations were conducted based on Scenario 3. A total of 20 random cases were considered. In each case, the initial vertical and lateral offsets were randomly selected within 4–8 m with random signs. The carrier airwake level was randomly selected from light, moderate, and heavy conditions, the airwake scaling factor was varied within 1.0–1.6, and the deck motion scale was varied within 0.9–1.2. In addition, lateral and vertical gusts with random injection times, durations, and amplitudes were imposed. The same random cases were applied to all compared controllers to ensure a fair comparison.

Figure 15 shows the boxplots of the IAE and RMS values for the position, velocity, and attitude tracking errors. For an error signal $e_i(t)$, these two indices are defined as $IAE_i = \int_0^{t_f} |e_i(t)| dt$ and $RMS_i = \sqrt{t_f^{-1} \int_0^{t_f} e_i^2(t) dt}$. In both Monte Carlo figures, the five-pointed stars denote the sample means. All controllers complete the landing task in the tested cases. Compared with FxTDI and NDI, PTDIAl produces more compact error distributions in most channels, especially for the vertical position tracking error e_z , velocity error e_V , and attitude errors. PTDIAl-NA shows similar lateral and attitude tracking performance to PTDIAl, but its vertical-channel performance is more sensitive to

actuator saturation. These results indicate that the proposed observer and predefined-time control structure improve the consistency of the closed-loop response under randomized disturbances.

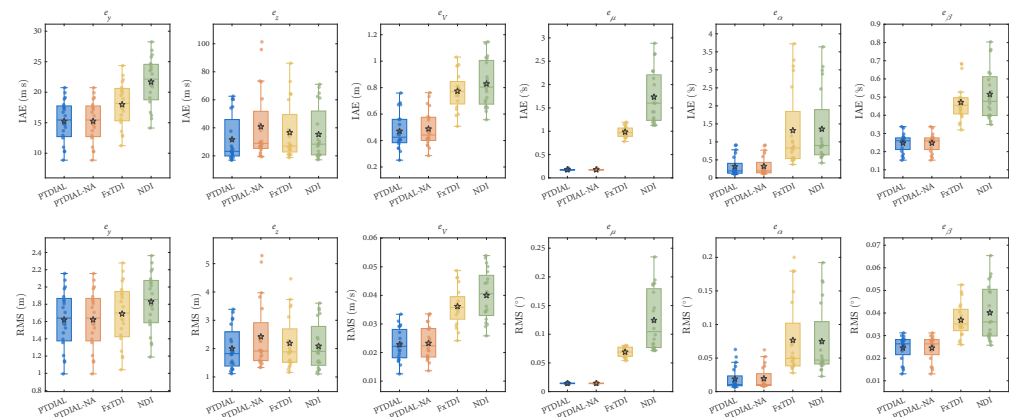


Figure 15. Monte Carlo comparison of IAE and RMS tracking metrics.

Additional quantitative metrics are further compared in Figure 16, including the terminal position errors, the maximum attitude errors, and the flap low-saturation time. PTDIAl maintains small terminal errors and reduced maximum attitude deviations over the tested cases. Although the lower-gain baseline controllers may naturally spend less time near the flap lower limit, their tracking and attitude metrics are generally less favorable. By comparing PTDIAl with PTDIAl-NA, it can be observed that the anti-saturation mechanism reduces the adverse influence of flap saturation on the vertical channel and the coupled longitudinal response. Therefore, the Monte Carlo results further confirm that PTDIAl provides a more reliable balance between tracking accuracy, disturbance rejection, and actuator feasibility.

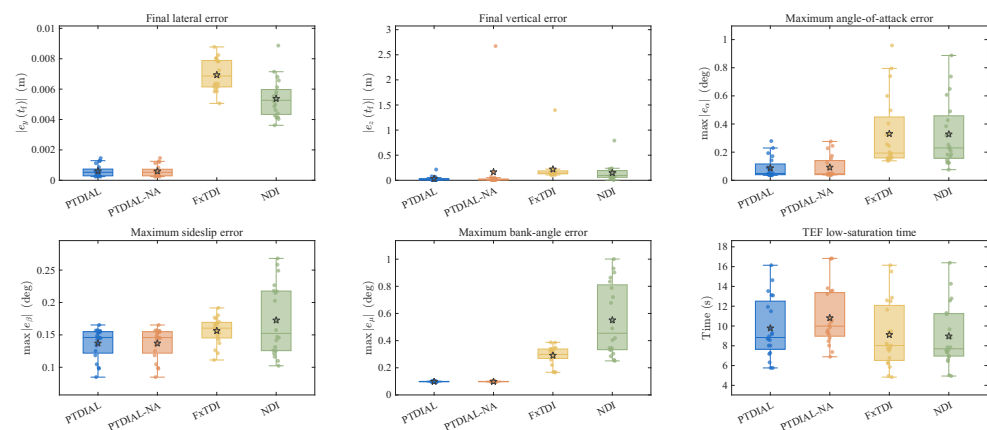


Figure 16. Monte Carlo comparison of quantitative metrics.

The numerical statistics corresponding to Figures 15 and 16 are summarized in Tables 3 and 4, respectively, as the mean $\pm$ standard deviation.

Table 3. Monte Carlo statistics of the tracking metrics.

Tracking Metric	PTDIAl	PTDIAl-NA	FxTDI	NDI
IAE of e_y (m/s)	15.246 $\pm$ 3.469	15.246 $\pm$ 3.469	17.988 $\pm$ 3.673	21.698 $\pm$ 4.024
IAE of e_z (m/s)	31.580 $\pm$ 16.628	40.821 $\pm$ 25.587	36.618 $\pm$ 20.594	35.311 $\pm$ 18.986
IAE of e_v (m)	0.471 $\pm$ 0.139	0.488 $\pm$ 0.136	0.774 $\pm$ 0.140	0.829 $\pm$ 0.179
IAE of e_μ (°s)	0.177 $\pm$ 0.014	0.176 $\pm$ 0.014	0.984 $\pm$ 0.116	1.735 $\pm$ 0.570
IAE of e_α (°s)	0.317 $\pm$ 0.280	0.322 $\pm$ 0.276	1.316 $\pm$ 1.106	1.356 $\pm$ 1.021
IAE of e_β (°s)	0.248 $\pm$ 0.051	0.248 $\pm$ 0.051	0.471 $\pm$ 0.102	0.515 $\pm$ 0.140

Table 3. *Cont.*

Tracking Metric	PTDIAL	PTDIAL-NA	FxTDI	NDI
RMS of e_y (m)	1.621 ± 0.340	1.621 ± 0.340	1.690 ± 0.359	1.830 ± 0.340
RMS of e_z (m)	2.002 ± 0.746	2.422 ± 1.214	2.189 ± 0.952	2.091 ± 0.835
RMS of e_V (m/s)	0.0228 ± 0.0057	0.0233 ± 0.0059	0.0361 ± 0.0067	0.0400 ± 0.0087
RMS of e_μ ($^\circ$)	0.0145 ± 0.0008	0.0145 ± 0.0008	0.0690 ± 0.0085	0.1243 ± 0.0526
RMS of e_α ($^\circ$)	0.0188 ± 0.0169	0.0194 ± 0.0168	0.0765 ± 0.0566	0.0747 ± 0.0527
RMS of e_β ($^\circ$)	0.0245 ± 0.0055	0.0245 ± 0.0055	0.0369 ± 0.0074	0.0401 ± 0.0123

Table 4. Monte Carlo statistics of the landing-related metrics.

Landing Metric	PTDIAL	PTDIAL-NA	FxTDI	NDI
$ e_y(t_f) $ (m)	0.00061 ± 0.00038	0.00060 ± 0.00038	0.00693 ± 0.00101	0.00537 ± 0.00127
$ e_z(t_f) $ (m)	0.0329 ± 0.0507	0.165 ± 0.626	0.220 ± 0.296	0.149 ± 0.176
$\max e_\alpha $ ($^\circ$)	0.087 ± 0.074	0.092 ± 0.079	0.331 ± 0.252	0.328 ± 0.233
$\max e_\beta $ ($^\circ$)	0.137 ± 0.024	0.137 ± 0.024	0.156 ± 0.021	0.173 ± 0.054
$\max e_\mu $ ($^\circ$)	0.0983 ± 0.0016	0.0983 ± 0.0016	0.291 ± 0.066	0.551 ± 0.259
TEF low-saturation time (s)	9.78 ± 3.05	10.81 ± 2.87	9.11 ± 3.34	8.97 ± 3.12

7. Conclusions

This paper proposed a predefined-time integrated direct lift/side-force control method for carrier-based fixed-wing UAV landing. The trailing-edge flap and spoiler were employed to generate direct lift and side force, respectively, allowing position corrections to be achieved with reduced attitude-mediated motion. A preview-based reference glide slope was combined with predefined-time control, prescribed-performance attitude regulation, predefined-time disturbance observation, and trailing-edge flap anti-saturation compensation. Theoretical analysis showed that the closed-loop tracking, filtering, observation, and auxiliary errors were practically predefined-time stable under the stated assumptions.

Simulation results demonstrated that the integrated direct-force architecture improved vertical and lateral tracking while maintaining small angle-of-attack, bank-angle, and sideslip variations. The proposed disturbance observer enhanced the rejection of airwake and gust disturbances, while the anti-saturation mechanism shortened the flap saturation duration and mitigated its coupled influence on the vertical channel. Comparisons with reduced direct-force configurations, fixed-time control, and nonlinear dynamic inversion confirmed the overall performance advantage of PTDIAL. The Monte Carlo results further demonstrated consistent tracking accuracy, attitude regulation, and actuator feasibility under randomized landing conditions. Future work will focus on aerodynamic uncertainty, multi-actuator control allocation, actuator faults, and hardware-in-the-loop or flight-test validation.

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Data Availability Statement: The data and simulation code used in this study are available from the corresponding author upon reasonable request.

DURC Statement: Current research is limited to theoretical and simulation-based automatic aircraft landing control, which is beneficial for improving landing safety and reliability and does not pose a threat to public health or national security. Authors acknowledge the dual-use potential of the research involving automatic carrier landing control and confirm that all necessary precautions have been taken to prevent potential misuse. As an ethical responsibility, the authors strictly adhere

to relevant national and international laws about DURC. The authors advocate for responsible deployment, ethical considerations, regulatory compliance, and transparent reporting to mitigate misuse risks and foster beneficial outcomes.

Conflicts of Interest: The authors declare no conflicts of interest.

Appendix A

Table A1. Abbreviations and symbols.

Notation	Definition
ITP	Ideal Touch Point
PTDIAL	Predefined-Time Direct Lift/Side-Force-Integrated Approach Landing
PPC, PTCE, PTDO	Prescribed performance control, predefined-time command filter, and predefined-time disturbance observer
$O_g x_g y_g z_g, O_b x_b y_b z_b, O_s x_s y_s z_s$	Inertial, body-fixed, and ship-fixed coordinate frames
$\mathbf{x}_1 = [y, z]^T$	Lateral and vertical position states
$\mathbf{x}_2 = [\chi, \gamma]^T$	Heading and flight-path angle states
$\mathbf{x}_3 = [\mu, \alpha, \beta]^T$	Bank angle, angle of attack, and sideslip angle states
$\mathbf{x}_4 = [p, q, r]^T$	Roll-, pitch-, and yaw-rate states
V, T	Airspeed and thrust
m, g	Aircraft mass and gravitational acceleration
$\mathbf{u}_2 = [\delta_s, \delta_{\text{tef}}]^T$	Differential-spoiler and trailing-edge-flap inputs
$\mathbf{u}_4 = [\delta_a, \delta_e, \delta_r]^T$	Aileron, elevator, and rudder inputs
L, D, Y	Lift, drag, and side force
$\bar{L}, \bar{M}, \bar{N}$	Roll, pitch, and yaw moments; $\bar{L}$ is distinguished from the lift force L by the overbar
d_2, d_4, d_V	Lumped disturbances in the trajectory, angular-rate, and velocity loops
$\bar{d}_j, \bar{v}_j$	Bounds on the magnitude and variation rate of the j th lumped disturbance
$\mathbf{V}_{\text{aw}}, \mathbf{V}_g, \mathbf{V}_w$	Carrier-airwake, gust, and total wind vectors
$\mathbf{P}_{\text{ITP}}, \mathbf{P}_{\text{ITP}}^0, \mathbf{R}_{\text{gs}}$	Instantaneous and nominal ITP positions and ship-to-inertial rotation matrix
$e_i, \rho_i, \xi_i, \varepsilon_i$	Tracking error, prescribed-performance function, normalized error, and transformed error
$(\hat{\cdot}), (\tilde{\cdot})$	Estimated quantity and estimation error, respectively
e_o, δ_o	Scalar state and disturbance estimation errors used in the PTDO analysis
σ_o, λ_o, r_o	Cross-term coefficient, dissipation margin, and residual radius in the PTDO analysis
$T_p, \varphi, \omega, \vartheta$	User-specified time parameter and the coefficients used in the practical predefined-time stability condition; the guaranteed convergence-time upper bound is $T_p / \sqrt{\vartheta}$
$(\cdot)^c, (\cdot)^r$	Commanded and reference quantities, respectively
$(\cdot)_{\text{aw}}, (\cdot)_g, (\cdot)_w$	Carrier-airwake, gust, and total-wind quantities, respectively
$(\cdot)_{\text{as}}$	Anti-saturation-related quantity

The nonlinear six-DOF model used in the simulation is summarized in this appendix. The physical and inertial parameters used in the simulations are listed in Table A2.

Table A2. Physical and inertial parameters of the UAV model.

Parameter	Value	Parameter	Value
Mass, m	14,000 kg	Wing reference area, S	37.16 m ²
Wing span, b	11.4 m	Mean aerodynamic chord, $\bar{c}$	3.488 m
Air density, ρ	1.225 kg/m ³	Gravitational acceleration, g	9.81 m/s ²
Roll moment of inertia, I_x	31,184 kg m ²	Pitch moment of inertia, I_y	156,910 kg m ²
Yaw moment of inertia, I_z	230,411 kg m ²	Product of inertia, I_{xz}	−4028 kg m ²

The translational and trajectory dynamics are given by

$$\begin{aligned}
 \dot{V} &= \frac{T \cos \alpha \cos \beta - D}{m} - g \sin \gamma + d_V, \\
 \dot{\chi} &= \frac{L \sin \mu + Y \cos \mu + T(\sin \alpha \sin \mu - \cos \alpha \sin \beta \cos \mu)}{mV \cos \gamma} + d_\chi, \\
 \dot{\gamma} &= \frac{L \cos \mu - Y \sin \mu + T(\sin \alpha \cos \mu + \cos \alpha \sin \beta \sin \mu) - mg \cos \gamma}{mV} + d_\gamma, \\
 \dot{x} &= V \cos \gamma \cos \chi, \quad \dot{y} = V \cos \gamma \sin \chi, \quad \dot{z} = -V \sin \gamma,
 \end{aligned} \tag{A1}$$

where T is the actual thrust, and g is the gravitational acceleration.

The physical control-surface realizations used in the simulations follow first-order dynamics subject to the position and rate constraints summarized in Table A3. In Figure 2, the engine dynamics from the thrust command T^c to the actual thrust T are represented by

$$G_p(s) = \frac{T(s)}{T^c(s)} = \frac{2.994s^3 + 10.48s^2 + 27.48s + 9.37}{s^4 + 6.5s^3 + 18.25s^2 + 26.28s + 9.37}.$$

The conventional-surface dynamics, position envelopes, and engine model were selected with reference to the carrier-landing models in [8,33], and the flap model was benchmarked against the actuator configuration in [2]. The direct-force actuator parameters used in the simulations are also stated explicitly in the table.

Table A3. Actuator and engine models and constraints.

Actuator	Dynamics	Position Limits	Rate Limits
Elevator	$\dot{\delta}_e = 30(\delta_e^c - \delta_e)$	[−0.42, 0.18] rad	±1.80 rad/s
Aileron	$\dot{\delta}_a = 48(\delta_a^c - \delta_a)$	[−0.44, 0.44] rad	±1.40 rad/s
Rudder	$\dot{\delta}_r = 40(\delta_r^c - \delta_r)$	[−0.52, 0.52] rad	±1.40 rad/s
Trailing-edge flap	$\dot{\delta}_{\text{tef}} = 30(\delta_{\text{tef}}^c - \delta_{\text{tef}})$	[−0.1396, 0.6981] rad	±1.0472 rad/s
Left/right spoiler	$\dot{\delta}_{sj} = 35(\delta_{sj}^c - \delta_{sj}), j \in \{L, R\}$	[0, 0.6109] rad	±1.0472 rad/s
Engine	$T(s) = G_p(s)T^c(s)$	$0 \leq T \leq 125,000$ N	–

The attitude kinematics and angular rate dynamics are expressed as

$$\begin{aligned}
 \dot{\mu} &= \frac{p \cos \alpha + r \sin \alpha}{\cos \beta} + \dot{\chi}(\sin \gamma + \tan \beta \sin \mu \cos \gamma) + \dot{\gamma} \tan \beta \cos \mu, \\
 \dot{\alpha} &= q - (p \cos \alpha + r \sin \alpha) \tan \beta + \frac{-\dot{\chi} \cos \gamma \sin \mu - \dot{\gamma} \cos \mu}{\cos \beta}, \\
 \dot{\beta} &= p \sin \alpha - r \cos \alpha + \dot{\chi} \cos \gamma \cos \mu - \dot{\gamma} \sin \mu, \\
 \dot{p} &= (c_1 r + c_2 p)q + c_3 \bar{L} + c_4 \bar{N} + d_p, \\
 \dot{q} &= c_5 p r - c_6 (p^2 - r^2) + c_7 \bar{M} + d_q, \\
 \dot{r} &= (c_8 p - c_2 r)q + c_4 \bar{L} + c_9 \bar{N} + d_r.
 \end{aligned} \tag{A2}$$

The Euler-angle relations used in the simulation are

$$\dot{\phi} = p + q \sin \phi \tan \theta + r \cos \phi \tan \theta, \quad \dot{\theta} = \dot{\gamma} + \dot{\alpha}, \quad \dot{\psi} = \dot{\chi} - \dot{\beta}. \tag{A3}$$

The aerodynamic forces and moments in Figure 2 are defined as

$$\begin{aligned}
 L &= \bar{Q} S C_L, & D &= \bar{Q} S C_D, & Y &= \bar{Q} S C_Y, \\
 \bar{L} &= \bar{Q} S b C_{\bar{L}}, & \bar{M} &= \bar{Q} S \bar{c} C_{\bar{M}}, & \bar{N} &= \bar{Q} S b C_{\bar{N}},
 \end{aligned} \tag{A4}$$

where L , D , and Y denote the lift, drag, and side force, respectively; $\bar{L}$, $\bar{M}$, and $\bar{N}$ denote the roll, pitch, and yaw moments, respectively. The overbar on $\bar{L}$ is used to distinguish the roll moment from the lift force L . In addition, $\bar{Q} = \rho V^2 / 2$ is the dynamic pressure, S is the wing reference area, b is the wing span, $\bar{c}$ is the mean aerodynamic chord, and ρ is the air density.

For the adopted UAV model, the aerodynamic coefficients are expressed as

$$\begin{aligned}
 C_L &= C_{L0} + C_{L\alpha} \alpha + C_{L\delta_e} \delta_e + C_{L\delta_{\text{tef}}} \delta_{\text{tef}} + C_{L\delta_s} \delta_{s\Sigma}, \\
 C_D &= C_{D0} + C_{D\alpha} \alpha + C_{D\delta_s} \delta_{s\Sigma}, \\
 C_Y &= C_{Y\beta} \beta + C_{Y\delta_s} \delta_{s\Delta}, \\
 C_{\bar{L}} &= C_{\bar{L}\beta} \beta + C_{\bar{L}\delta_a} \delta_a + C_{\bar{L}\delta_r} \delta_r + C_{\bar{L}\delta_s} \delta_{s\Delta} + \frac{b}{2V} (C_{\bar{L}p} p + C_{\bar{L}r} r), \\
 C_{\bar{M}} &= C_{\bar{M}0} + C_{\bar{M}\alpha} \alpha + C_{\bar{M}\delta_e} \delta_e + C_{\bar{M}\delta_{\text{tef}}} \delta_{\text{tef}} + \frac{\bar{c}}{2V} C_{\bar{M}q} q, \\
 C_{\bar{N}} &= C_{\bar{N}\beta} \beta + C_{\bar{N}\delta_a} \delta_a + C_{\bar{N}\delta_r} \delta_r + C_{\bar{N}\delta_s} \delta_{s\Delta} + \frac{b}{2V} (C_{\bar{N}p} p + C_{\bar{N}r} r).
 \end{aligned} \tag{A5}$$

The numerical coefficients corresponding to Equation (A5) are listed in Table A4. All angular variables and control-surface deflections in these expressions are measured in radians.

Table A4. Numerical aerodynamic coefficients used in the simulations.

Component	Aerodynamic Coefficients
Lift	$C_{L0} = 0.8229$, $C_{L\alpha} = 2.7584$, $C_{L\delta_e} = 0.4894$, $C_{L\delta_{\text{tef}}} = 0.6000$, $C_{L\delta_s} = -0.1200$
Drag	$C_{D0} = 0.1027$, $C_{D\alpha} = 0.7476$, $C_{D\delta_s} = 0.0800$
Side force	$C_{Y\beta} = -0.8022$, $C_{Y\delta_s} = 0.3000$
Roll moment	$C_{\bar{L}\beta} = -0.1203$, $C_{\bar{L}\delta_a} = 0.1834$, $C_{\bar{L}\delta_r} = 0.0172$, $C_{\bar{L}p} = -0.3800$, $C_{\bar{L}r} = 0.0130$, $C_{\bar{L}\delta_s} = 0.0420$

Table A4. Cont.

Component	Aerodynamic Coefficients
Pitch moment	$C_{M0} = 0.0603, C_{M\alpha} = -0.4260, C_{Mq} = -3.5432,$ $C_{M\delta_e} = -0.6596, C_{M\delta_{\text{tef}}} = -0.0200$
Yaw moment	$C_{N\beta} = 0.0974, C_{N\delta_a} = 0.0115, C_{N\delta_r} = -0.0859,$ $C_{Np} = -0.0500, C_{Nr} = -0.2400, C_{N\delta_s} = -0.0300$

The spoiler variables used in the aerodynamic model are defined as

$$\delta_{s\Sigma} = \frac{1}{2}(\delta_{sL} + \delta_{sR}), \quad \delta_{s\Delta} = \delta_{sR} - \delta_{sL}. \quad (\text{A6})$$

In the trajectory-loop vector input u_2 in Equation (1), δ_s denotes the direct side-force command and is implemented as the differential spoiler deflection, i.e., $\delta_s \equiv \delta_{s\Delta}$. The symmetric spoiler component $\delta_{s\Sigma}$ mainly affects lift and drag, whereas $\delta_{s\Delta}$ generates direct side force and lateral-directional moments.

The signed differential command is allocated to the physical spoilers by

$$(\delta_{sL}^c, \delta_{sR}^c) = \begin{cases} (0, \min\{\delta_s^c, \delta_{s,\max}\}), & \delta_s^c \geq 0, \\ (\min\{-\delta_s^c, \delta_{s,\max}\}, 0), & \delta_s^c < 0, \end{cases} \quad \delta_{s,\max} = 0.6109 \text{ rad.}$$

The allocation satisfies $0 \leq \delta_{sL}^c, \delta_{sR}^c \leq \delta_{s,\max}$. For $|\delta_s^c| \leq \delta_{s,\max}$, the resulting components are $\delta_{s\Delta}^c = \delta_s^c$ and $\delta_{s\Sigma}^c = |\delta_s^c|/2$. Both components are retained in the aerodynamic coefficients in Equation (A5).

The wind-induced equivalent disturbance terms are computed from the angle-of-attack and sideslip perturbations α_w and β_w . The corresponding aerodynamic increments are

$$\begin{aligned} L_w &= \bar{Q}SC_{L\alpha}\alpha_w, & D_w &= \bar{Q}SC_{D\alpha}\alpha_w, & Y_w &= \bar{Q}SC_{Y\beta}\beta_w, \\ \bar{L}_w &= \bar{Q}SbC_{\bar{L}\beta}\beta_w, & \bar{M}_w &= \bar{Q}S\bar{c}C_{\bar{M}\alpha}\alpha_w, & \bar{N}_w &= \bar{Q}SbC_{\bar{N}\beta}\beta_w. \end{aligned} \quad (\text{A7})$$

Thus, the lumped disturbances are

$$\begin{aligned} d_V &= \frac{-Y\beta_w + L\alpha_w - D_w}{m}, \\ d_\chi &= \frac{(L_w + D\alpha_w)\sin\mu + (Y_w - D\beta_w)\cos\mu}{mV\cos\gamma}, \\ d_\gamma &= \frac{(L_w + D\alpha_w)\cos\mu + (D\beta_w - Y_w)\sin\mu}{mV}, \\ d_p &= c_3\bar{L}_w + c_4\bar{N}_w, & d_q &= c_7\bar{M}_w, & d_r &= c_4\bar{L}_w + c_9\bar{N}_w. \end{aligned} \quad (\text{A8})$$

The inertia-related coefficients in the angular rate equations are

$$\begin{aligned} c_1 &= \frac{(I_y - I_z)I_z - I_{xz}^2}{I_x I_z - I_{xz}^2}, & c_2 &= \frac{(I_x - I_y + I_z)I_{xz}}{I_x I_z - I_{xz}^2}, & c_3 &= \frac{I_z}{I_x I_z - I_{xz}^2}, \\ c_4 &= \frac{I_{xz}}{I_x I_z - I_{xz}^2}, & c_5 &= \frac{I_z - I_x}{I_y}, & c_6 &= \frac{I_{xz}}{I_y}, \\ c_7 &= \frac{1}{I_y}, & c_8 &= \frac{I_x(I_x - I_y) + I_{xz}^2}{I_x I_z - I_{xz}^2}, & c_9 &= \frac{I_x}{I_x I_z - I_{xz}^2}. \end{aligned} \quad (\text{A9})$$

For reproducibility, the principal numerical settings of the proposed controller and the PTDOs are summarized below. For all controller channels, the common double-power parameters were $\varphi = 0.45$, $m = 12$, and $n = 4$, and the regularization threshold was

$\epsilon_\ell = 10^{-3}$. The predefined-time parameters are listed in Table A5; entries within each vector follow the channel order shown in the second column.

Table A5. Predefined-time parameters of the controller.

Control Loop	Channels	T (s)
Position	(y, z)	(7, 11)
Trajectory	(χ, γ)	(5, 8)
Attitude	(μ, α, β)	(6, 6, 5)
Angular rate	(p, q, r)	(3, 3, 3)
Velocity	V	10

For all PTDO channels, the common double-power parameters were $\varphi_o = 0.45$, $m_o = 1.30$, and $n_o = 0.85$. The remaining channel-dependent parameters are listed in Table A6.

Table A6. Channel-dependent parameters of the PTDOs.

Channels	T_o (s)	k_l	k_s	k_d	ϵ_o
(χ, γ)	(1.00, 0.90)	(0.20, 0.05)	(0.82, 0.25)	(0.14, 0.25)	$(5 \times 10^{-8}, 10^{-8})$
(p, q, r)	(0.75, 0.65, 0.65)	(0.24, 0.24, 0.25)	(0.82, 0.90, 0.90)	(0.24, 0.26, 0.26)	$(10^{-6}, 5 \times 10^{-7}, 10^{-8})$
V	0.75	0.10	0.90	0.28	10^{-6}

Table A7. Settings of the short-term AR predictor.

Setting	Value
AR model order, p	10
Number of historical samples, N	20
Historical-data interval, Δt_{AR}	0.5 s
Prediction steps, n_f	3
Prediction horizon, T_{pre}	1.5 s

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