

Nonlinear Model Predictive Control for Hybrid Flapping-Rotor Micro Aerial Vehicles

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To enhance the aerodynamic efficiency of Micro Aerial Vehicles (MAVs) with rotary wings, a bio-inspired Hybrid Flapping-Wing Rotor (HFWR) configuration can be designed which achieves a power efficiency more than twice that of conventional rotors. Nevertheless, up to the present, the controllable flight of HFWR has so far eluded realisation due to severe flapping-induced structural vibrations and nonlinear coupling between aerodynamic and elastic dynamics. This paper provides a practical step of stable, controllable HFWR flight through two key innovations: a thrust-vectoring gimbal architecture that delivers continuous control moments under strong oscillations, and an Enhanced nonlinear Model Predictive Control (E-MPC) framework implemented as a distributed two-layer architecture. In this architecture, the outer layer consists of a lower-rate offboard MPC that generates constraint-aware attitude trim and bias commands, while the inner layer is a high-rate onboard proportional angular-rate loop that provides rapid damping of high-frequency perturbations caused by flapping-induced vibrations and communication or optimization latency. Hover and Yaw flight tests demonstrate that the integrated architecture improves attitude stability compared with cascade PID and a baseline offboard MPC without the onboard rate loop, reducing peak deviation, overshoot and steady-state error by up to 83%, 92% and 80%, respectively, while substantially lowering control energy. These results demonstrate a

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practical pathway towards stable control of flapping-rotor MAVs for the first time, bridging the gap between bio-inspired aerodynamic efficiency and flight controllability.

Nomenclature

C_s	=	Structural damping coefficient (Ns/m)
f	=	Flapping frequency (Hz)
$F_{gim,rot}$	=	Thrust generated by the gimbal rotor (N)
$F_{xyz,b,gim}$	=	Force of gimbal rotor system (N)
$F_{(x/y/z),b}$	=	Forces in the body coordinate system (x/y/z-axis), respectively, (N)
$F_{xyz,b}$	=	Resultant force vector in the body coordinate system(N)
$F_{xyz,b,FWR}$	=	Force contributions of the Hybrid FWR (N)
$F_{xyz,b,gim}$	=	Force vector from the gimbal rotor (N)
$F_{xyz,b,flap}$	=	Force vector from the flapping wing (N)
F_1	=	Total thrust/vibration force from the main FWR (N)
F_2, F_3	=	Gimbal rotor thrust component in the x- and z-direction (N)
F_2^*	=	Optimized value of F_2 from MPC optimization (N)
$\widetilde{F}_3^*$	=	Adjusted optimized value of F_3 after local P-controller compensation(N)
$F_{excite}(t)$	=	Excitation force from flapping (N)
$F_{xyz,b,flap}$	=	Disturbance force vector from flapping vibrations(N)
I_w	=	Rotational inertia of the wing (kgm ²)
K_s		Structural stiffness (N/m)
K_θ, K_φ		Structural stiffness (N/m)
l_b	=	Distance from the CG to the tail rotor (m)
$M_{xyz,b,tail}$	=	Torque from the tail rotor (Nm)
$M_{xyz,b,FWR}$	=	Moment contributions of the Hybrid FWR (Nm)
$M_{xyz,b}$	=	Resultant moment vector in the body coordinate system (Nm)

$M_{xyz,b,gim}$	=	Moment vector from the gimbal rotor (Nm)
$M_{xyz,b,tail}$	=	Torque from the tail rotor (Nm)
$M_{xyz,b,flap}$	=	Disturbance moment vector from flapping vibrations (Nm)
$M_{gim,rot}$	=	Torque produced by the gimbal rotor (Nm)
N	=	Finite prediction horizon
r_1, r_2	=	Linkage and wing lengths, respectively (m)
$T_{central,p}$	=	Duty cycle at the servo's neutral position (%)
$Thr(\%)$	=	Duty cycle percentage (%)
T_{min}, T_{max}	=	Minimum and Maximum duty cycle percentages, respectively (%)
T_s	=	Sampling time interval (s)
$T_{PWM,p}$	=	Duty cycle percentage (%)
w_x, w_z	=	Weight coefficients for x- and z- direction displacement, respectively
w_θ, w_φ	=	Weight coefficients for pitch and roll angle errors, respectively
w_u	=	Weight coefficient for control input (thrust)
x	=	State vector (position, attitude, velocity)
$x_{gim}, y_{gim}, z_{gim}$	=	Position of the gimbal rotor along the x-, y- and z-axes in the body coordinate system(m)
x_{ref}	=	Reference x-position (m)
z_f	=	vertical displacement of the fuselage in the vibration mode (m)
z_{k+j}	=	Predicted z-altitude in the MPC prediction horizon (m)
z_{ref}	=	Reference z-altitude (m)
Δt	=	Sampling interval (s)
θ, φ	=	Pitch and roll attitude angles of aircraft, respectively, (deg)
$\theta_{gim,max}, \theta_{gim,min}$	=	Maximum and Minimum allowable servo angles, respectively (deg)
θ_{k+j}	=	Predicted pitch angle at the $(k + j)$ step ahead of the current time (deg)
$\theta_{gim,p}$	=	Gimbal angle (deg)

$\theta_p(t)$	=	Servo's current angle at time t (deg)
θ_{ref}	=	Reference pitch angle (deg)
$\theta_{target,p}$	=	Target angle of servos (deg)
$\theta_{gim,p}$	=	Gimbal angle (deg)
τ	=	Servo response time constant (s)
ψ	=	Rotor azimuth angle (deg)
ω	=	Angular frequency (rad/s)

I. Introduction

Micro Aerial Vehicles (MAVs) typically refer to Rotary-wing aircraft with a wingspan smaller than 30 cm [1–6]. They offer tremendous potential in both military and civilian domains. However, due to the constraints imposed by current battery energy densities and hardware performance, their flight endurance remains below 30 minutes and can drop to under 5 minutes under heavy payload conditions [1,7,8]. Consequently, enhancing the power efficiency of MAVs within the limits of existing battery and hardware technologies is a critical focus of contemporary research.

As a consequence, Cranfield University proposed the Flapping Wing Rotor (FWR) concept in 2009 [9–11], integrating bio-inspired flapping with standard rotor rotation. Under specific operating conditions, the FWR demonstrated a 1.43-fold improvement in power efficiency compared to traditional rotors. Building upon this concept, the authors further introduced the Hybrid FWR (HFWR) in Refs. [12,13], which leverages large-amplitude, high-frequency flapping to enhance lift and overall efficiency, achieving a remarkable 2.14-fold efficiency boost relative to conventional rotors. Nevertheless, the combination of large flapping amplitudes and flexible structural elements can induce severe aerodynamic and structural elastic effects, generating intense vibrations and disturbance torques throughout the structure dynamics, thereby compromising attitude stability. Over the past 16 years, multiple attempts [14–17] employing conventional controllers have been made to counter these strong nonlinear couplings and high-amplitude oscillations, yet none have successfully enabled stable, controllable flight in the FWR family. The difficulty is rooted in the distinctive dynamics introduced by the FWR mechanism. Unlike conventional rotary-wing MAVs, where rotor aerodynamics can often be approximated by quasi-steady momentum theory, the FWR couples large-amplitude flapping with (passive or semi-passive) rotation, yielding strongly time-varying aerodynamic states. Each

wingbeat produces transient lift and thrust while simultaneously injecting torque into the hub, driving the rotational motion and altering the instantaneous flow field. As a result, the airframe is subjected to highly oscillatory force profiles and disturbance torques associated with unsteady flow phenomena (vortex formation and wake interactions), which propagate through the lightweight structure as significant vibratory loads. The vibration-driven deformation and sensor contamination further aggravate stabilization, as high-frequency oscillations can corrupt inertial measurements and reduce the effective phase margin of feedback loops.

These characteristics have consistently challenged classical PID or cascaded-loop control strategies. In practice, the combination of rapid, oscillatory disturbances and strong nonlinear coupling can lead to overshoot amplification and slow recovery, or even divergence, when the controller bandwidth is limited by computation, sensing, or actuation. Only a small number of experimental studies have attempted “controllable” FWR flight. For example, Dong [17] and co-workers demonstrated a PID-based solution using stabilizers/rudders, shown in Fig. 1, to exploit the rotor downwash for generating aerodynamic moments, achieving acceptable tracking in forward-flight commands but exhibiting difficulty in reverse commands and pronounced overshoot and sluggish recovery under vibratory loads; the authors attributed part of this limitation to centre-of-gravity offsets and asymmetric mass distribution. In parallel, they proposed a visual-inertial fusion framework [18] to mitigate vibration-induced drift by combining high-rate inertial sensing with delayed but accurate external visual feedback, highlighting that state estimation and control are both fundamentally constrained by the oscillatory environment. Collectively, prior results suggest that stable, multi-channel, long-duration controllability has remained elusive for the FWR family, not merely due to controller tuning, but due to insufficient and vibration-sensitive control authority together with severe unmodeled dynamics and sensing challenges.

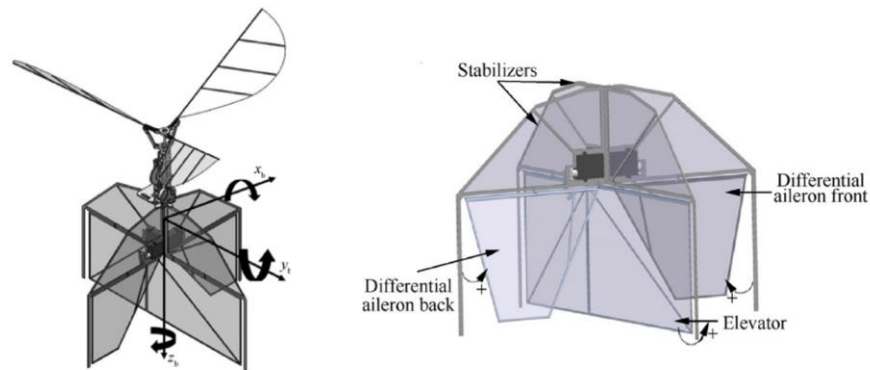


Fig. 1 Schematic diagram of Dong's controllable FWR solution

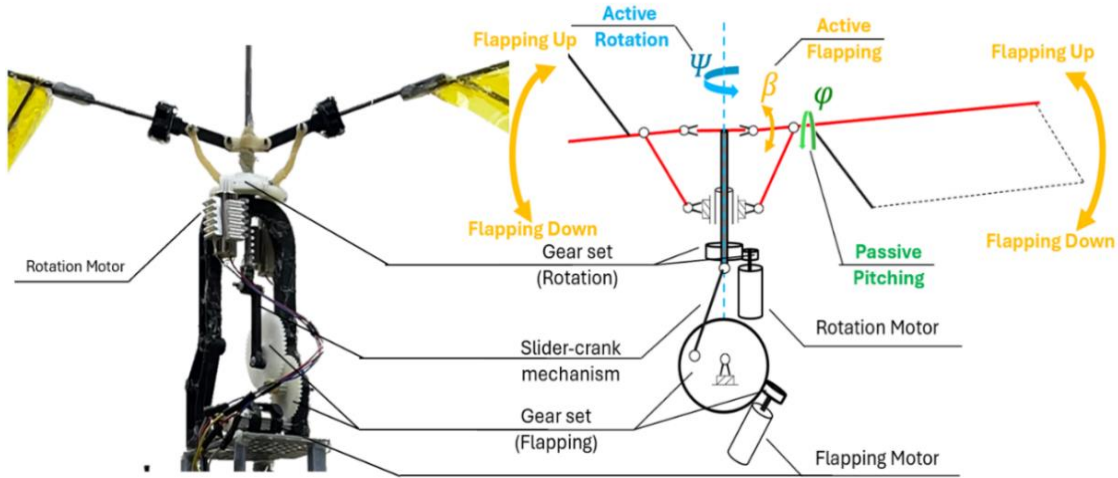


Fig. 2 Schematic illustration of the HFWR concept design

As MAV applications diversify, Model Predictive Control (MPC) has gained considerable traction in both academia and industry for managing high-frequency vibrations, nonlinear dynamics, and significant aerodynamic disturbances [19–21]. MPC has also been successfully applied to hybrid and multi-mode UAVs, where it enables stable control across highly nonlinear transition regimes and coupled flight mode [22]. By continuously solving an optimization problem within a finite prediction horizon, MPC can deliver near-optimal control performance in multi-constraint, high-uncertainty environments [23]. However, when faced with severe, nonlinear flapping-induced vibrations, traditional MPC approaches exhibit sluggish responses or insufficient adaptability to disturbances due to their lengthy optimization computation times and high computational resource demands [24]. In fact, real-time execution of nonlinear MPC generally demands computational resources on the order of a single-board embedded computer, owing to the repeated solution of nonlinear constrained optimization problems at each control step. This level of onboard computation is impractical for direct deployment on an HFWR MAV with a total mass of only 110 g, given its stringent constraints on weight and energy availability.

Motivated by these considerations, this study tackles the intense vibration and high-disturbance control issues of HFWR MAVs by proposing an enhanced nonlinear MPC based control architecture. The proposed approach retains the predictive and rolling-optimization strengths of nonlinear MPC, particularly its ability to handle time-varying constraints, low-frequency attitude regulation, and trim/bias compensation while introducing architectural innovations that enable distributed computation and practical implementation on a lightweight HFWR MAV. In the resulting two-layer structure, the offboard MPC primarily provides constraint-aware attitude commands and suppresses low-frequency drift, whereas a high-rate onboard angular-velocity proportional loop supplies rapid damping against high-

frequency oscillations and mitigates the effects of communication and optimization latency. Compared to baseline methods, including cascade PID (C-PID) and standard offboard MPC without onboard rate feedback (S-MPC), the integrated scheme demonstrates notable improvements in peak deviation, overshoot, steady-state accuracy, and energy consumption, thereby presenting a practical engineering solution for stable, controllable flight in HFWR MAVs.

Meanwhile, analysis reveals that controller design alone is insufficient: within the HFWR's severe, nonlinear, flapping-induced vibration environment, the vehicle must continuously generate a stable attitude-control moment, a challenge that every successful flapping-wing MAV has met through either passive or active stabilization. The RoboBee [25], for instance, achieves passive stabilization with orthogonally crossed fins above and below the body whose aerodynamic drag produces a restoring moment, whereas the DelFly Nimble [26] uses active stabilization by placing a servo-driven wing-root adjustment mechanism in the low-vibration root region to vary each wing's angle of attack in real time. Neither strategy can be transplanted to the HFWR of Fig. 2: violent structural oscillations quickly destabilize passive fins, and the rotor's simultaneous flapping and rotation leave no mount point for a wing-root servo because its leads cannot wrap around the spinning hub. Consequently, a completely new HFWR airframe capable of delivering continuous, stable control moments under extreme oscillation must be devised, and this requirement, together with the limitations of existing controllers, motivates the two objectives of the present work: to design such an airframe, and to develop an MPC-based control algorithm that maintains HFWR stability under the same harsh conditions.

The structure of this paper is as follows. Section II presents the full-vehicle configuration of the HFWR platform, followed by the development of its dynamics model in Section III. Section IV addresses the dynamic characteristics of the HFWR MAV, highlighting the challenges introduced by structural coupling and vibrations due to flapping rotors. This analysis motivates a series of enhancements to the nonlinear MPC framework, which is detailed thereafter. In Section V, the proposed approach is validated through a combination of comparative experiments and real-world flight tests, demonstrating notable improvements in control performance. Finally, Section VI concludes the paper by summarizing key findings and outlining directions for future research.

II. HFWR Configuration Design

The concept of the new HFWR architecture proposed in Ref. [13] is shown in Fig. 2. When the HFWR rotor is operating, in addition to the conventional helicopter-like rotation generated by the rotational motor on the rotor, the

electric flapping motor initiates a periodic blade-flapping motion. Once flapping begins, a specialized bio-inspired unsteady aerodynamic phenomenon described (Delayed Stall, Weis-Fogh) in Refs. [27,28] is generated. By decoupling the flapping frequency from the rotor rotational speed, and by means of a dedicated method that controls the two rates independently, this HFWR design enables the rotor to reach a high-efficiency operating point across a wide range of flow conditions, achieving a power efficiency of 2.14 times higher than that of a traditional rotor [13]. This outcome endows the HFWR with significant adaptability to real-world flight scenarios and lays the groundwork for implementing controllable flight in practical vehicles.

However, despite the added motor and the decoupling of flapping and rotation, the HFWR rotor still incorporates a complex, large-amplitude flapping mechanism, so integrating a conventional swashplate for attitude control remains impractical. Therefore, attitude control for aircraft using the HFWR concept must rely on alternative structural and control solutions. Accordingly, a controllable HFWR configuration has been designed, as shown in Fig. 3.

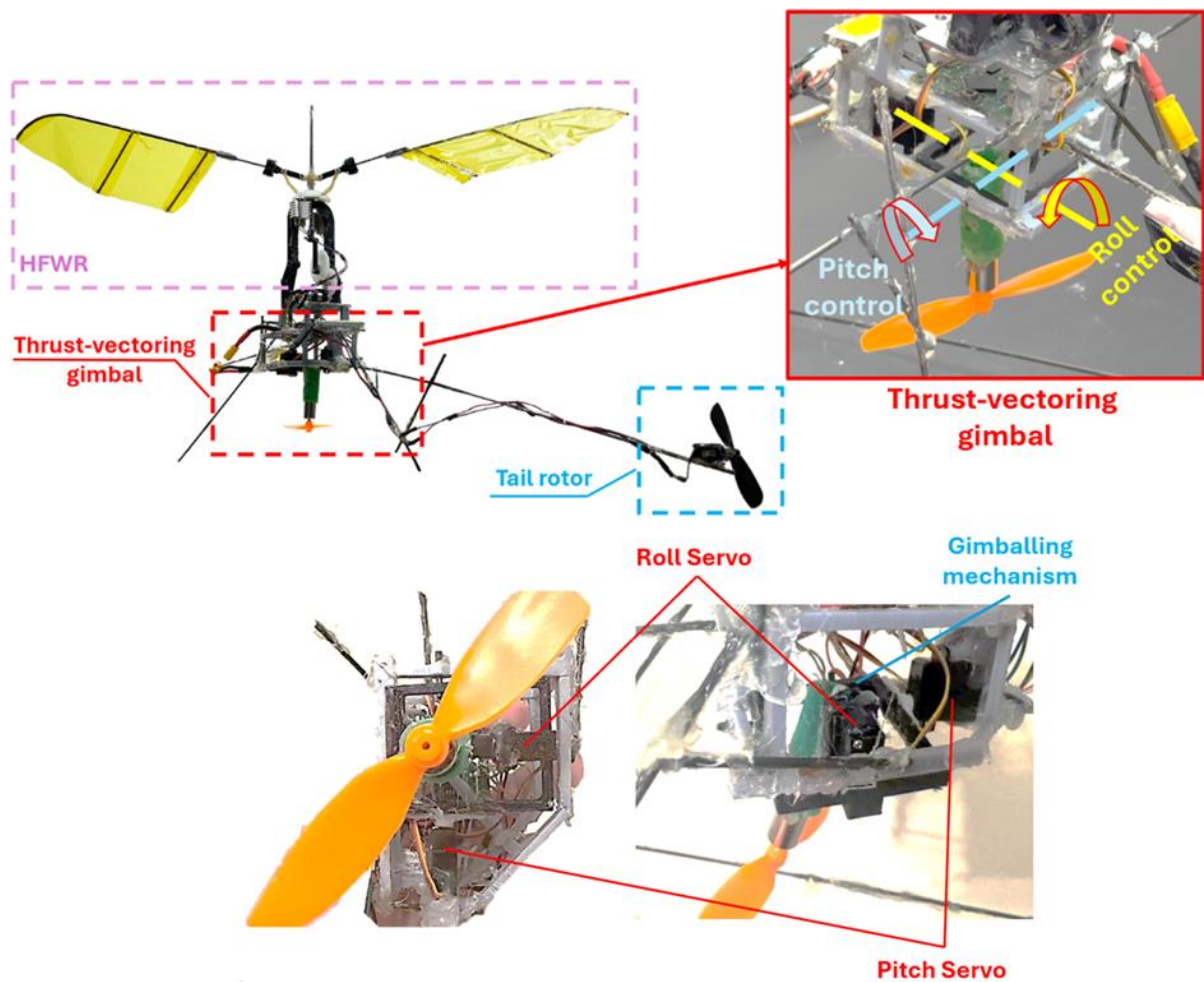


Fig. 3 Illustration of the full-size HFWR configuration and its thrust-vectoring gimbal

In this configuration, the HFWR supplies the primary lift. A tail rotor provides yaw-axis control torque by varying its speed, while a thrust-vectoring gimbal delivers continuous, stable control moments for attitude regulation. This gimbal subsystem consists of a small rotor mounted on a gimbal that can pivot about the pitch and roll axes. Similar to a gimbaled rocket engine nozzle, deflecting this rotor produces both forces and torques that stabilize and maneuver the aircraft, eliminating the need for a conventional swashplate. The functional block diagram for using the gimbal to achieve forward and backward flight is shown in Fig. 4. By tilting the gimbal angle θ_{gim} , control moments that pitch the aircraft up or down are generated, enabling forward and backward motion; lateral flight is achieved in the same way.

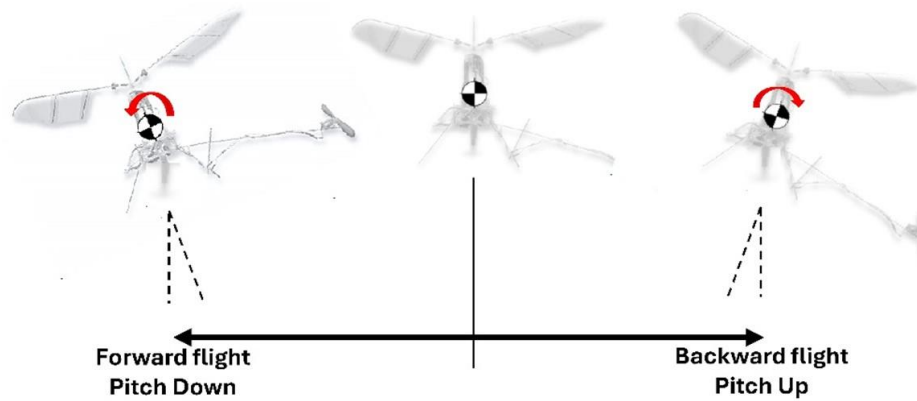


Fig. 4 Thrust vectoring gimbal to achieve forward and backward flight of HFWR

Since the thrust-vectoring gimbal operates independently of the flapping mechanism, it maintains consistent torque outputs even under the high-amplitude vibrations induced by HFWR flapping, remaining unaffected by them. Two servomotors with response times of approximately 0.04 s per 60° control the thrust vectoring gimbal's rotor angles, allowing for adjusting the attitude at frequencies of up to 25 Hz - significantly exceeding the typical 4–8 Hz adjustment rates achieved by conventional helicopter swashplates [29]. This high-frequency actuation makes the thrust vectoring gimbal ideally suited for attitude control in the new design.

In addition to the thrust vectoring gimbal for pitch and roll stability, a tail rotor, shown in Fig. 5, is installed to govern yaw control. Positioned at the end of a 2 mm diameter carbon fiber rod, this single-direction tail rotor provides torque about the yaw axis. While it imparts a constant yawing force in one direction, counter-torque from the HFWR's main rotor and the thrust vectoring gimbal are employed to manage yaw motion in the opposite direction.

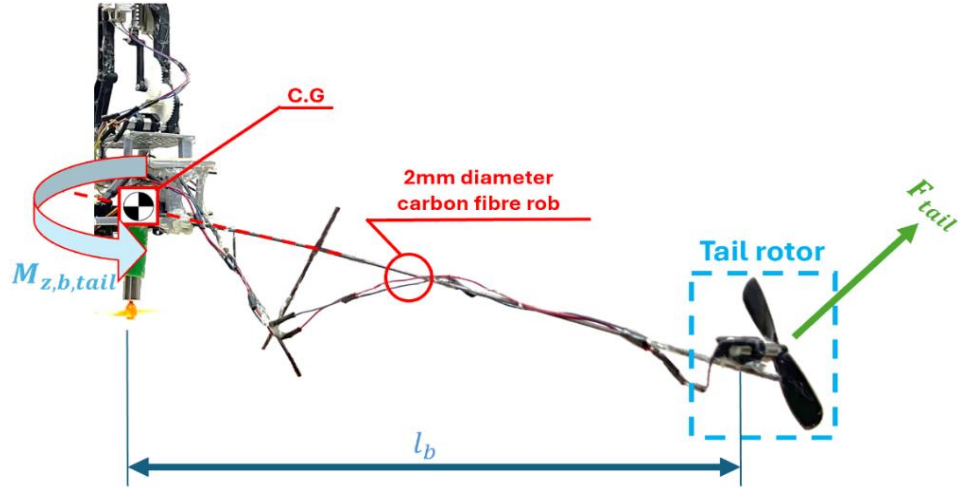


Fig. 5 Tail rotor of HFWR MAV

To enable effective flight control, a dedicated mounting platform for the flight-control board is installed directly beneath the main rotor hub. Positioning the flight controller close to the vehicle's center of gravity (CG) enhances the accuracy of attitude measurements. The custom-designed flight-control unit named BioMAV-controller weighs only 3.4 g and utilizes an ESP32 microcontroller, which supports both Wi-Fi and Bluetooth communication and operates at frequencies up to 240 MHz. This compact, high-performance controller includes drivers for up to four coreless motors, accommodates sixteen servo channels, and can sustain a maximum operating current of 20 A. Additionally, it provides real-time wireless communication capability, enabling hardware-in-the-loop (HIL) simulations [30–34]. Studies by [35,36] indicate that this configuration offers superior performance compared to most flight-control units of comparable size. The flight controller and its installation position are illustrated in Fig. 6. Table 1 summarizes the performance parameters of BioMAV-Controller.

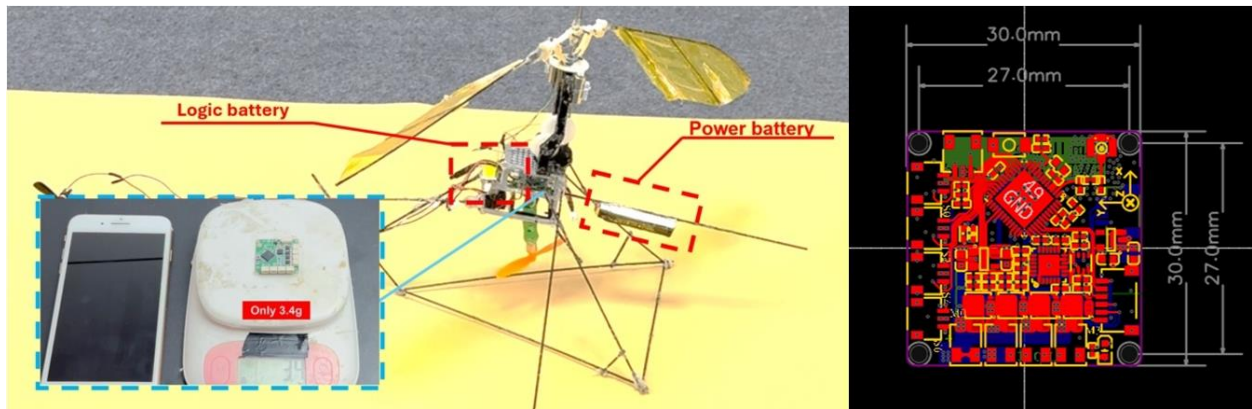


Fig. 6 Flight controller of HFWR MAV

Table 1 BioMAV Controller Performance Parameters

Specification	Details
Weight	3.4g
Dimensions	3cm × 3cm × 2mm
Main Control Chip	ESP32
Gyroscope	MPU6050
Barometer	BMP280
Motor Support	4 × hollow cup motors
Servo Support	8 channels
Communication	2.4GHz WiFi + Bluetooth
Power Supply Voltage	4.2V
Maximum Current	20A

The HFWR MAV uses a dual-battery power architecture to maintain stable operation of both the propulsion system and the onboard electronics. A dedicated power battery drives the motors and servomechanisms, while a separate logic battery supplies the flight-control processor and sensors. Because the vehicle's nominal bus voltage (3.7 V) is nearly identical to that required by the ESP32 controller, a single-battery configuration is prone to voltage sag and back-EMF transients generated during motor speed changes. These fluctuations can induce controller brownouts and degrade sensor signal integrity. Electrical separation of the loads reduces these disturbances and prevents under-voltage faults. The power battery is positioned forward to place the center of gravity above the gimballed rotor, yielding an appropriate longitudinal mass distribution.

The dimensions of the HFWR configuration are shown in Fig. 7, and the values are defined in Table 2.

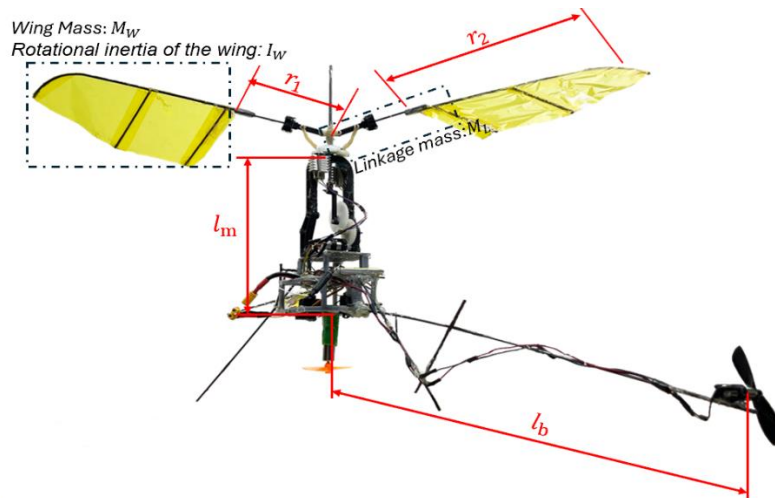
**Fig. 7 HFWR configuration structure size definition**

Table 2 Principal physical parameters of HFWR MAV

Parameter	Symbol	Value	Unit
Mass Distribution			
Body mass	M_s	0.110	kg
Linkage mass	M_L	0.003	kg
Wing mass	M_w	0.010	kg
Length			
Linkage	r_1	0.020	m
Wing	r_2	0.150	m
Tail	l_b	0.3	m
height	l_m	0.1	m
Inertia Parameters			
Rotational inertia of the wing	I_w	0.000653	$\text{kg} \cdot \text{m}^2$

III. Modelling of HFWR MAV

A. Overview of modelling components

A complete mathematical dynamic model of the HFWR MAV, based on the configuration described in Section II, is required to characterize the vehicle's response and to assess the performance of the MPC controller in both analytical and simulation environments. The key task is the formulation of subsystem models capable of predicting the forces and moments acting on the vehicle with sufficient fidelity. Once these subsystems are defined, the resulting aerodynamic and control loads can be computed to form the basis for subsequent dynamic analysis. Accordingly, this section presents the development of three principal subsystem models that govern the HFWR MAV's force and moment generation: the HFWR aerodynamic model, the thrust-vectoring gimbal model, and the tail-rotor model. The modelling framework of the HFWR MAV is illustrated in Fig. 8.

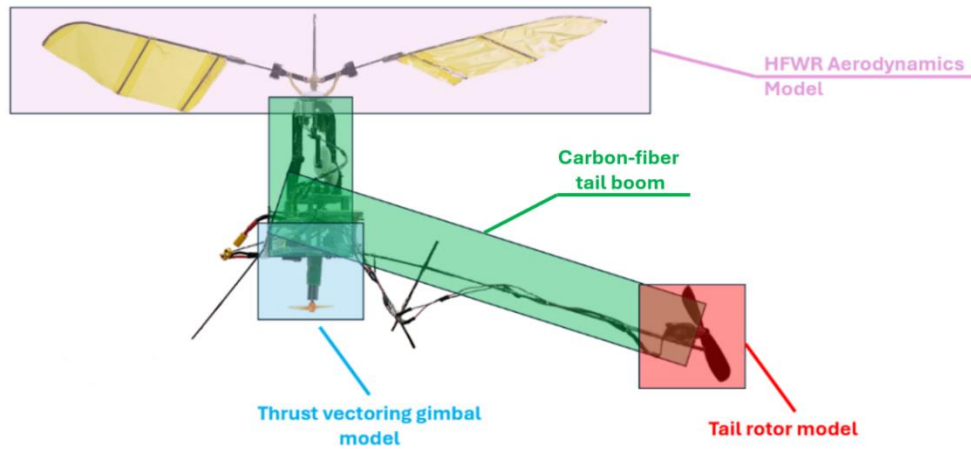


Fig. 8 Illustration of the HFWR MAV model

It is worth noting that the HFWR aerodynamic model involves coupled flapping-rotational kinematics, aeroelasticity, and structural elasticity, making it highly complex. Its fidelity even requires physical correction techniques for improved modeling accuracy. This model is reported in Ref. [13] and will not be elaborated upon in this paper. The following sections will instead focus on the modeling of the remaining two components that have a direct impact on the forces and moments of the HFWR MAV: the thrust vectoring gimbal and the tail rotor.

B. Modeling of HFWR components

The thrust vectoring gimbal is a critical component responsible for the attitude stabilization and control of the HFWR MAV. This rotor is actuated by an 8520 coreless motor, whose power and thrust output are directly correlated with the duty cycle percentage (flight control program) applied by the flight control hardware, as shown in Fig. 9.

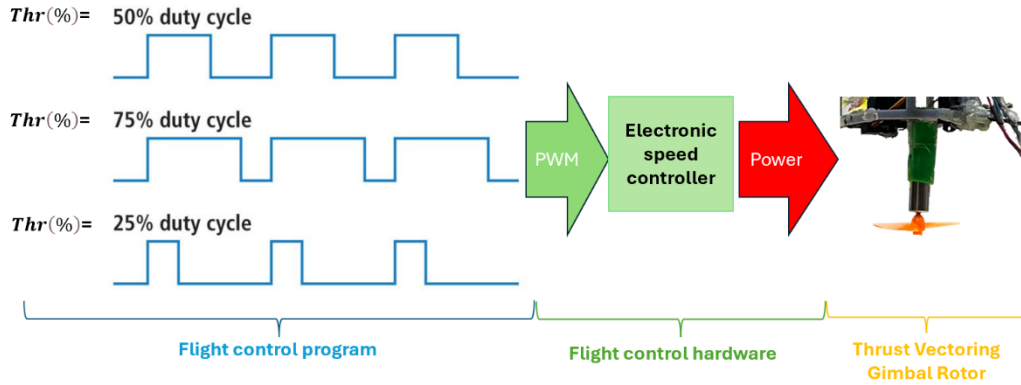


Fig. 9 Control process of the thrust vectoring gimbal power and thrust

As the duty cycle percentage $Thr(\%)$ increases, the rotor accelerates, generating greater thrust. The relationship between $Thr(\%)$ and the thrust force $F_{gim,rot}$ and torque $M_{gim,rot}$ produced by the gimbal rotor, as determined through experimental data fitting, is given by:

$$\begin{cases} F_{gim,rot} = 0.351 \times Thr(\%) \\ M_{gim,rot} = 0.032 \times Thr(\%) \end{cases} \quad (1)$$

Since the thrust direction of the rotor is governed by the gimbal mechanism, which is actuated by servos controlled via duty cycle modulation, the servo angle $\theta_p(t)$ and the applied duty cycle percentage $T_{PWM,p}$ exhibit a functional relationship:

$$\begin{cases} \theta_{target,p} = \frac{(T_{PWM,p} + T_{central,p}) - T_{min}}{T_{max} - T_{min}} \cdot (\theta_{gim,max} - \theta_{gim,min}) + \theta_{gim,min} \\ \theta_p(t) = \theta_{target,p} + [\theta_p(0) - \theta_{target,p}] e^{-\frac{t}{\tau}} \end{cases} \quad (2)$$

in which T_{min} and T_{max} are the minimum and maximum duty cycle percentages, set to 16% and 83% in this study. $\theta_{gim,min}$ and $\theta_{gim,max}$ define the minimum and maximum gimbal angles, set to $\theta_{gim,min} = 30^\circ$, $\theta_{gim,max} = 150^\circ$ according to our structure design. $\theta_p(0)$ represent servo's initial angle (deg) at $t = 0$, $\theta_{target,p}$ is the target angle(deg) of servos, and τ denotes an equivalent first-order actuator time constant used for control-oriented modeling. Based on the servo specification (0.04s/60°at 4.2V), τ is selected as $\tau = 0.01s$ in this study. $T_{central,p}$ corresponds to the duty cycle at the servo's neutral position, set to 50%.

At $T_{PWM,p} = 0$, the gimbal aligns vertically with the ground, maintaining a neutral angle of $\theta_p(t \rightarrow \infty) = \theta_{target,p} = 90^\circ$ in Eq. (2). Considering this neutral servo angle $\theta_p(t \rightarrow \infty) = 90^\circ$ currently as the initial angle $\theta_{gim,p} = 0^\circ$ of the thrust vector $F_{gim,rot}$, the conversion relationship between $\theta_p(t)$ and $\theta_{gim,p}$ is

$$\theta_{gim,p} = \theta_p(t) - 90^\circ \quad (3)$$

By combining the equations from Eqs. (1) to (3) and accounting for time delay, which is a critical consideration, as neglecting latency would result in unrealistically ideal servo dynamics during virtual simulations and controller design, the gimbal servo angle $\theta_{gim,p}$ is given by:

$$\theta_{gim,p} = \theta_{target,p} + [\theta_p(0) - \theta_{target,p}] e^{-\frac{t}{\tau}} - 90^\circ \quad (4)$$

With the thrust model $F_{gim,rot}$, moment model $M_{gim,rot}$, and gimbal angle model $\theta_{gim,p}$ established, the forces and moments generated by the thrust vectoring gimbal system are transformed into the HFWR MAV's body coordinate frame as follows:

$$F_{xyz,b,gim} = \begin{bmatrix} F_{x,b} \\ F_{y,b} \\ F_{z,b} \end{bmatrix} = \begin{bmatrix} \sin(\theta_{gim,0}) \cdot \cos(\theta_{gim,1}) \\ -F_{gim,rot} \cdot \sin(\theta_{gim,0}) \\ \cos(\theta_{gim,0}) \cdot \sin(\theta_{gim,1}) \end{bmatrix} F_{gim,rot} \quad (5)$$

$$M_{xyz,b,gim} = \begin{bmatrix} 0 \\ 0 \\ M_{gim,rot} \end{bmatrix} + \begin{bmatrix} 0 & z_{gim} & y_{gim} \\ z_{gim} & 0 & -x_{gim} \\ -y_{gim} & x_{gim} & 0 \end{bmatrix} \begin{bmatrix} F_{x,b} \\ F_{y,b} \\ F_{z,b} \end{bmatrix} \quad (6)$$

Here, $\theta_{gim,0}$ and $\theta_{gim,1}$ denote the gimbal deflection angles generated by the two orthogonal servos, where $\theta_{gim,0}$ corresponds to the pitch-axis tilting servo and $\theta_{gim,1}$ corresponds to the roll-axis tilting servo. Since the tail boom passes directly through the HFWR MAV CG, as shown in Fig. 5, the thrust generated by the tail rotor primarily contributes to moments along the z -axis in the body coordinate frame of HFWR. This moment can be simplified as:

$$M_{xyz,b,tail} = \begin{bmatrix} 0 \\ 0 \\ 0.15 \cdot Thr(\%) \cdot l_b \end{bmatrix} \quad (7)$$

in which l_b represents the distance from the CG to the tail rotor along the boom, as detailed in Table 2.

After completing the development of the subsystem model, the next key focus is to design the flight attitude controller to achieve stable flight of the HFWR MAV under structure oscillation conditions.

IV. Development of Enhanced Nonlinear MPC

The decision to adopt MPC as the primary controller for the HFWR MAV is driven by two main considerations. First, numerous previous studies on FWR MAVs [17], have revealed that traditional single-loop or C-PID controllers perform inadequately in addressing control challenges related to the structure oscillation phenomenon induced by flapping and high levels of external disturbances, making it difficult to achieve stable attitude control. Second, as a modern control approach, MPC has been widely applied to various highly oscillatory and disturbance-prone motion scenarios [37,38], such as bipedal [39] and quadrupedal robots [40], and has demonstrated robust and effective performance, leading to its mature and broad adoption. Following this line of reasoning, the oscillatory environment encountered by the HFWR bears a notable resemblance to that of bipedal robotic systems. Therefore, MPC holds significant potential for application in its control. To facilitate the effective design of the MPC controller, it is first essential to assess the severity of the HFWR structure oscillation phenomenon in Section III.A, establishing the necessity of implementing MPC, highlighting the existing research gap, and justifying our control strategy. For this purpose, a detailed experimental analysis of the HFWR structure oscillation phenomenon was conducted, as described in Section III.B.

A. Assessment of the structure oscillation phenomenon of HFWR

The structure oscillation issue of the HFWR largely stems from the extreme demand for weight minimization in bio-inspired MAV design. To minimize weight, the design adopts an array of lightweight materials. Fig. 10 depicts the material-to-component distribution within the aircraft.

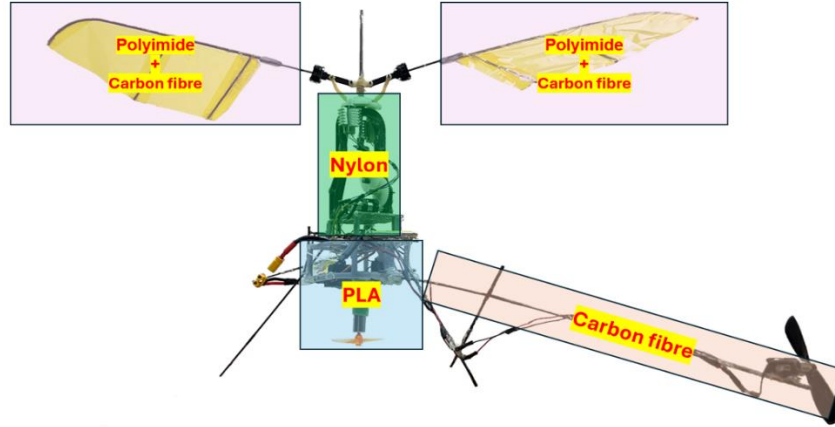


Fig. 10 Material-to-component distribution within HFWR MAV

Despite careful structural design aimed at minimizing mechanical stress, the relatively low stiffness inherent to these lightweight materials can still result in significant oscillations under flapping-induced loads, as illustrated in Fig. 11. The high-speed camera captures slow-motion footage illustrating the fuselage's vertical oscillation. The red line there is a simplified representation of the fuselage deformation obtained by image analysis, highlighting the importance of incorporating these vibratory effects into the system's modelling and control design. In practice, the airframe frequently experiences large-amplitude oscillations, with angular deviations reaching up to $\pm 33^\circ$. Such vibrations, primarily excited by the flapping mechanism, introduce considerable disturbance forces and moments, significantly complicating the stability of the HFWR MAV.

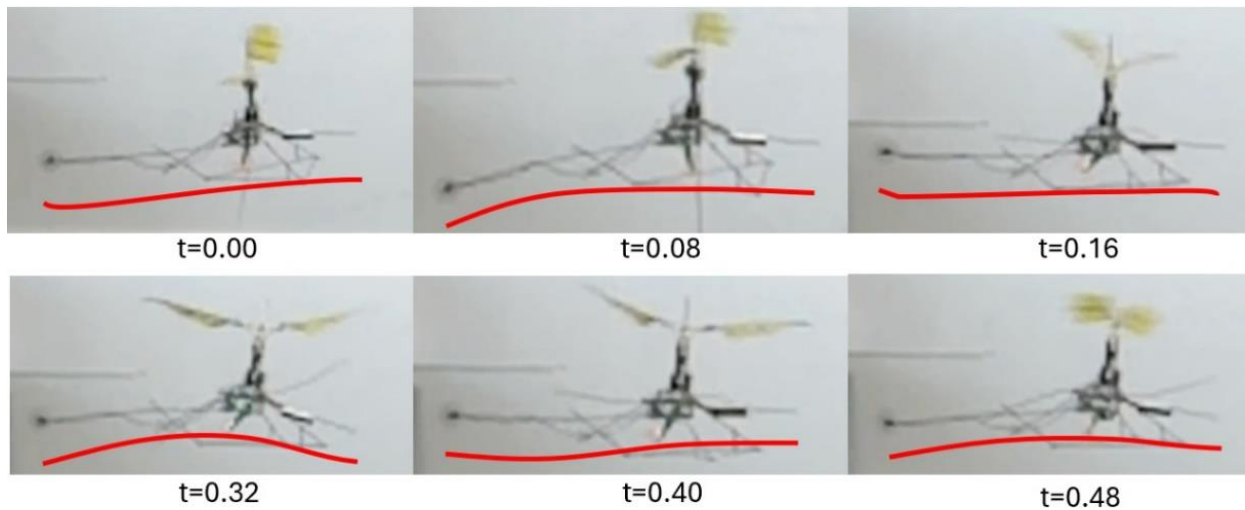


Fig. 11 Fuselage's vertical oscillation of HFWR

Through multiple experiments conducted on the platform, as shown in Fig. 12 [12], measurements were collected under conditions where rotor rotation was disabled, and the azimuth angle was fixed at 90 degrees in Fig. 13. These

measurements captured the structure oscillatory force and torque acting on the x_b -axis induced by the 0 - 20 Hz structure oscillation phenomena. The measurement results are presented in Fig. 14.

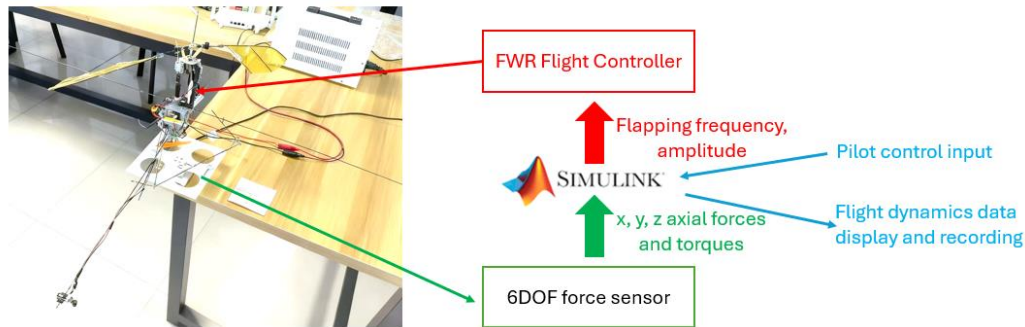


Fig. 12 The force measurement platform of HFWR

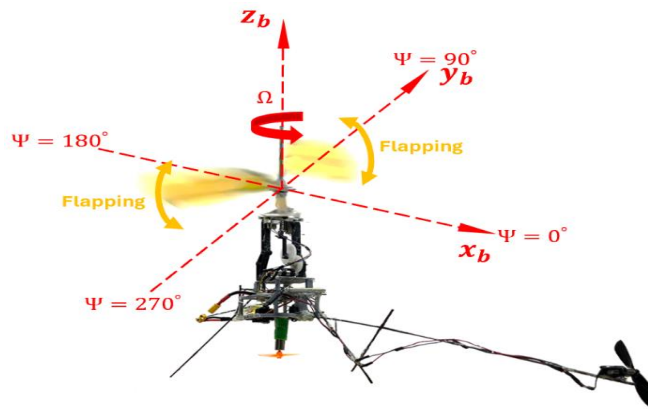


Fig. 13 The flapping wings of the HFWR flap along the x_b axis ($\psi = 90^\circ$)

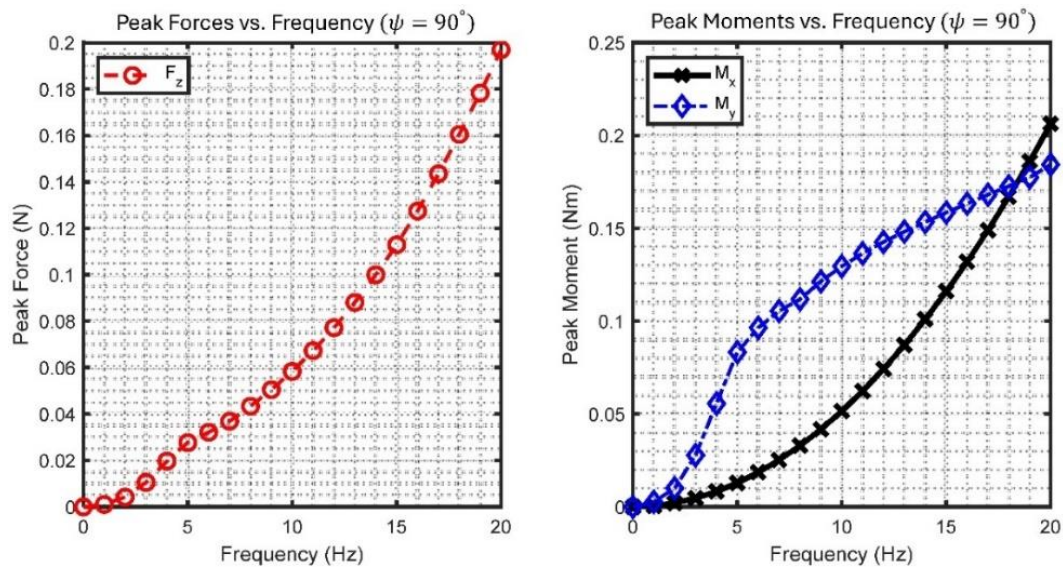


Fig. 14 HFWR 0-20Hz force and torque oscillation amplitude ($\psi = 90^\circ$)

Fig. 14 demonstrates that the forces and moments induced by structural oscillations are considerable in the HFWR MAV. At a vibration frequency of 20 Hz, the rotor can generate a peak oscillation force of 0.20 N, which corresponds to approximately 18.5% of the vehicle weight. Simultaneously, a peak oscillation torque of 0.21 Nm can induce an instantaneous angular velocity of up to approximately 1.72 rad/s in an aircraft weighing only 110 grams, making the roll attitude around the body-fixed x_b axis highly susceptible to divergence.

Therefore, it is essential to adopt advanced control strategies such as MPC, which can predict future system behavior and dynamically adjust control inputs to effectively mitigate the attitude disturbances caused by strong structural oscillations. Traditional PID controllers are incapable of forecasting system states and thus cannot "anticipate" the potential instability in the next moment of the HFWR MAV, making them less suitable for such highly dynamic and disturbance-prone environments.

B. Enhanced MPC control diagram

To address the challenges highlighted in Section IV.A and enable reliable control of the HFWR MAV under intense structural oscillations, this study proposes an enhanced MPC (E-MPC) framework specifically tailored to the unique characteristics of the HFWR MAV, as illustrated in Fig. 15.

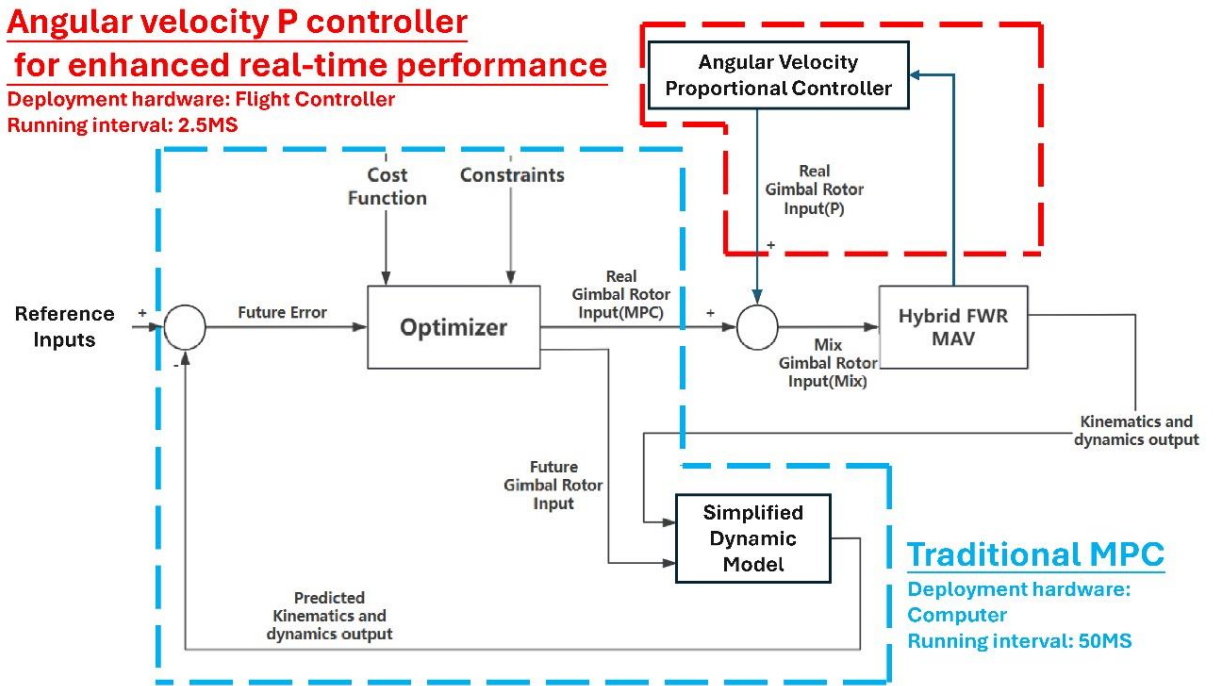


Fig. 15 E-MPC controller for HFWR MAV

The control architecture integrates a conventional MPC controller with a high-frequency angular velocity proportional (P) controller to enhance real-time responsiveness. The traditional MPC module, delineated by the blue dashed box in Fig. 15, operates at a relatively low update frequency of 50ms on a ground-based (offboard) computing platform. Due to the small size ($46\text{cm} \times 6\text{cm} \times 26\text{cm}$) and weight (110g) constraints of the HFWR MAV, it is not feasible to embed an onboard processor capable of running computationally intensive MPC algorithms, which typically require a Python environment and significant computational resources. Consequently, the MPC must execute externally on a ground station, and its output is transmitted wirelessly to the flight controller, as shown in Fig. 16.



Fig. 16 E-MPC implementation framework on HFWR MAV

This externally run MPC within the blue-dashed box in Fig. 15 utilizes a dynamic model of the MAV to predict future states, optimize a cost function under system constraints, and generate a sequence of thrust vectoring gimbal commands. This predictive capability allows it to account for the periodic structure oscillation induced by flapping-wing motion and adjust control inputs proactively. However, based on extensive engineering trials, its low update rate limits its responsiveness to the rapid angular velocity fluctuations that are inherent in the HFWR MAV's oscillatory structure. This limitation arises from both the computational delay of MPC's rolling optimization and the wireless communication latency, which has been measured to exceed 25ms [41]. Together, these factors extend the total control loop interval beyond 50ms, significantly impairing flight stability and automatic control performance of HFWR MAV.

To mitigate these limitations, a high-speed angular velocity P controller, shown in the red dashed box in Fig. 15, is introduced. This controller is deployed directly on the onboard flight controller unit in Fig. 16 and operates at a much faster rate (2.5ms). It provides immediate compensation for high-frequency angular velocity deviations. The P controller's output is then blended with the MPC's thrust vectoring gimbal control commands to form a unified control input. This hybrid control strategy combines the predictive optimization strengths of the MPC with the rapid corrective feedback of the P controller, resulting in significantly improved attitude stabilization even under strong structural oscillation.

This dual-controller scheme thus leverages the long-horizon prediction capability of MPC and the fast local feedback of the high-rate P controller to offer a robust and high-performance control solution for the strong structural oscillation HFWR MAV. The implementation logic and technical details of these components will be detailed in the following sections.

C. Simplified dynamic model for E-MPC

The first and most critical component of the MPC controller is the Simplified Dynamic Model (Fig. 15) used for its implementation. This model must accurately capture the essential dynamic behavior of the vehicle while maintaining high computational efficiency. Such efficiency is crucial to allow sufficient time for multi-step state prediction and receding-horizon optimization, ultimately yielding the optimal thrust-vectoring gimbal commands. Accordingly, the dynamic model employed in the MPC framework is a reduced-order version of the full model established in Section III. It should be emphasized that the purpose of this simplified model is not to reproduce all high-frequency aerodynamic and structural phenomena of the HFWR. Flapping-induced periodic forces, structural elasticity, and time-varying disturbances are intentionally not modeled in detail, because these effects are difficult to parameterize reliably for hand-fabricated platforms with significant unit-to-unit variability (manually manufactured flexible wings, adhesive joints, and structural tolerances). Even when detailed models are available, their parameters are often difficult to identify experimentally and may not generalize across different airframes. Instead, the control architecture is designed such that different disturbance regimes are handled by different components. The simplified model used in MPC primarily captures the low-frequency attitude dynamics that dominate trim, drift, and constraint-aware command generation, while high-frequency oscillatory disturbances are attenuated by the onboard high-rate angular-velocity feedback loop. In this sense, the MPC acts as a predictive low-frequency planner and constraint-aware regulator, whereas the inner-loop controller injects damping to compensate for unmodeled fast dynamics. The robustness of this approach is supported by experimental results showing stable flight despite strong unmodeled oscillations. A systematic sensitivity analysis with respect to modeling uncertainty would be valuable but was beyond the scope of the present experimental campaign and is identified as an important direction for future work. The development of this Simplified Dynamic Model is described below.

To achieve this reduction, the full dynamic model presented in Section III, which includes pitch, roll, and yaw dynamics, is simplified to retain only the pitch and roll channels. This configuration enables the E-MPC framework

to employ two independent MPC controllers for the pitch and roll axes, respectively: an offboard MPC integrated with the onboard pitch-rate controller, and another offboard MPC paired with the onboard roll-rate controller. Because structural oscillations predominantly affect these two axes [13], while the yaw motion remains largely decoupled, yaw control is implemented separately through a conventional proportional angular-velocity loop. The overall input–output structure of the three control channels (pitch, roll, and yaw) is illustrated in Fig. 17.

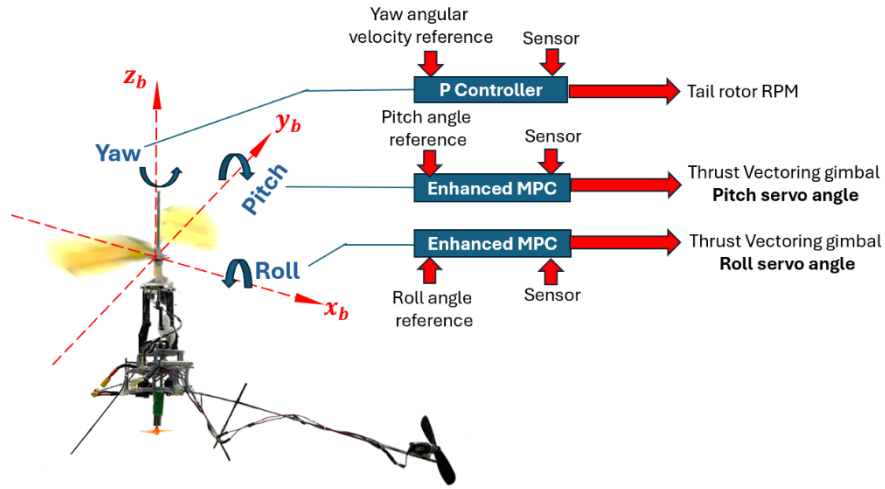


Fig. 17 HFWR MAV Pitch, Roll, and Yaw Controllers and Outputs

The Simplified Dynamic Model is developed for the pitch axis as a representative case, while the roll-axis formulation, being analogous, is omitted for brevity. The rotational motion of the vehicle about the pitch axis corresponds to its movement in the $X - O - Z$ plane. A schematic diagram of this motion is shown in Fig. 18.

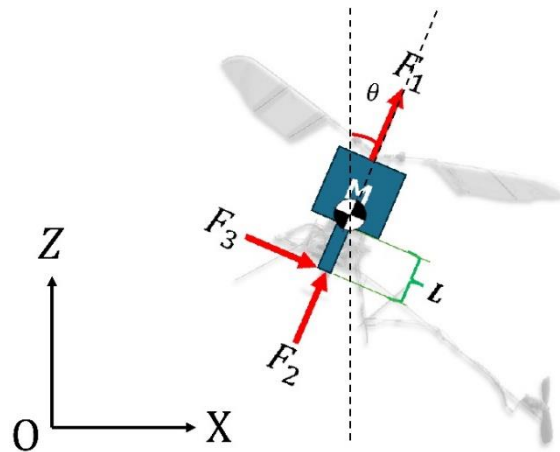


Fig. 18 Simplified HFWR pitch dynamic model

The HFWR in Fig. 18 can move along the horizontal $X - axis$ and vertical $Z - axis$, with a pitch angle θ denoting MAV body tilt in the $X - O - Z$ plane. Thus, following Newton's laws and rigid-body rotation principles, write the equations of motion as:

$$\begin{cases} \ddot{x} = \frac{F_1 \sin(\theta) + F_2 \sin(\theta) + F_3 \cos(\theta)}{M_s} \\ \ddot{z} = \frac{F_1 \cos(\theta) + F_2 \cos(\theta) - F_3 \sin(\theta) - M_s g}{M_s} \\ \ddot{\theta} = L \frac{F_3}{I_b} \end{cases} \quad (8)$$

where F_1 is the thrust from the two flapping wings of the HFWR, F_2 and F_3 are the decomposed forces of the thrust vectoring gimbal acting on the HFWR body, M_s is the total mass, g is gravitational acceleration, and I_b is the moment of inertia of the gimbal about the centre of mass (with a distance L). Defining the state vector

$$x = [x, z, \theta, \dot{x}, \dot{z}, \dot{\theta}]^T, \quad (9)$$

and viewing the thrust components F_2 and F_3 as control inputs $u = [F_2, F_3]^T$, we convert the continuous-time model into a discrete-time form suitable for digital implementation. A straightforward forward-Euler approach with a sampling interval T_s yields,

$$x_{k+1} = f(x_k, u_k), \quad (10)$$

where x_{k+1} is the predicted next state given the current state x_k and the control input u_k . Fig. 19 depicts how the HFWR MAV's simplified forces are mapped to the thrust vectoring gimbal.

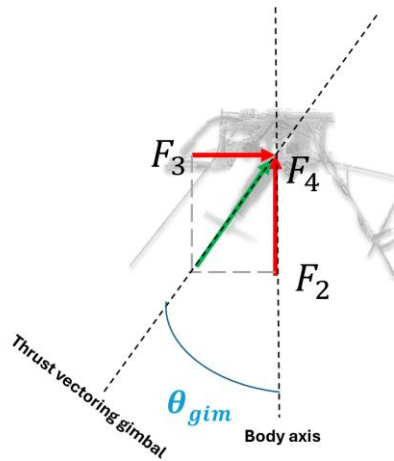


Fig. 19 HFWR's forces F_2 and F_3 mapped to the thrust vectoring gimbal pitch angle θ

In Fig. 19, the thrust of the gimbal $F_4 = \sqrt{F_2^2 + F_3^2}$, and $\theta_{gim} = \tan^{-1} \frac{F_2}{F_3}$, representing the pitch angle servo of the gimbal. By decomposing F_4 into F_2 and F_3 , and optimizing them directly within the optimizer, the overall computational complexity is significantly reduced. This approach eliminates the need to evaluate additional trigonometric functions, such as tangent or arctangent, during both model computation and real-time optimization of the gimbal. The gimbal rotor force components required to achieve this optimized control target are defined as F_2^* and F_3^* , thus $F_4^* = \sqrt{F_2^{*2} + F_3^{*2}}$. With the corresponding angular displacement denoted as $\theta_{gim}^* = \tan^{-1} \frac{F_2^*}{F_3^*}$, determines the gimbal rotor's pitch angle and requires thrust for achieving optimized control. These data will be transmitted to the flight controller in Fig. 16 for flight control execution.

The preceding formulation describes the development of the Simplified Dynamic Model used in the E-MPC for the pitch axis. This reduced-order model enables fast prediction of the HFWR MAV's pitch dynamics at each time step, providing the foundation for optimal control within the MPC framework. The modeling of the roll dynamics follows the same principles and procedures and is therefore omitted here for brevity.

D. Optimizer component

Building upon the simplified dynamic model established in Section IV.C, this section introduces the optimizer component of the E-MPC architecture, as illustrated in Fig. 15. The optimizer lies at the core of the MPC framework, tasked with determining an optimal sequence of control inputs over a finite horizon to guide the system towards the desired state. It consists primarily of a cost function that encodes control objectives, alongside a set of constraints that ensure the feasibility of control commands within physical and operational limits.

At each control cycle, the system first acquires or estimates the current state vector x_k , and then projects a series of future states x_{k+1} using the discrete-time model in Eq. (10). Given a finite prediction horizon N (set to 3 in the HFWR MAV to ensure real-time solvability), the optimizer explores a sequence of candidate control inputs $\{u_k, u_{k+1}, \dots, u_{k+N-1}\}$. Among these, it selects the optimal sequence $\{u_k^*, u_{k+1}^*, \dots, u_{k+N-1}^*\}$ that minimizes a specifically designed cost function. Only the first control action u_k^* is applied to the system, and the optimization process repeats at the next step in a receding-horizon fashion.

The design of the cost function is critical to aligning control efforts with mission objectives. For the HFWR, Fig. 15 reveals that maintaining attitude stability, particularly pitch angle and angular velocity, is paramount due to the

platform's inherent oscillatory tendencies. Therefore, the cost function prioritizes pitch-related terms while also penalizing position deviations and excessive control effort. A quadratic cost function is adopted:

$$J = \sum_{j=0}^{N-1} \left[w_x (x_{k+j} - x_{ref})^2 + w_z (z_{k+j} - z_{ref})^2 + w_\theta (\theta_{k+j} - \theta_{ref})^2 + w_{\dot{\theta}} (\dot{\theta}_{k+j} - (\theta_{k+j} - \theta_{ref}) / \Delta t)^2 + w_u (F_{2,k+j}^2 + F_{3,k+j}^2) \right] \quad (11)$$

in which w_x , w_z , w_θ , $w_{\dot{\theta}}$, and w_u are weight coefficients that reflect the relative importance of each term, as specified in Table 3. Δt is the sampling interval. The weight coefficients for the roll channel are included as well. Reference values x_{ref} , z_{ref} , θ_{ref} define the target state along the X and Z axes and the desired pitch attitude, respectively.

Table 3 HFWR MPC weight coefficient setting

Weight Definition	Symbol	Numeric
X-axis displacement weight	w_x	1000
Y-axis displacement weight	w_y	100
Z-axis displacement weight	w_z	1000
Pitch Attitude angle weight	w_θ	3000
Pitch Attitude angular velocity weight	$w_{\dot{\theta}}$	5
Roll Attitude angle weight	w_ϕ	2000
Roll Attitude angular velocity weight	$w_{\dot{\phi}}$	5
Gimbal rotor excessive control weight	w_u	1

The cost function assigns substantially higher weights to the pitch and roll attitude angles, ensuring the controller suppresses pitch oscillations before engaging in precise position tracking. Furthermore, the penalty on control effort $w_u (F_{2,k+j}^2 + F_{3,k+j}^2)$ limits aggressive thrust commands that could saturate the gimbal servo (whose maximum actuation rate is constrained to 60%/0.04s) or drain power resources prematurely .

By optimizing the predicted input sequence under these criteria, the E-MPC computes the desired gimbal thrust components (F_2^*, F_3^*) , which are subsequently translated into the gimbal servo angle θ_{gim} and motor speed commands for flight execution using F_4 . These optimized inputs not only maintain stability in the face of aerodynamic disturbances and structure-induced oscillations but also ensure that the system remains within its mechanical and power limits. The final weight coefficients, tuned through iterative simulation and flight testing, are also included in Table 3.

In addition to the cost function, the MPC solver enforces physical constraints:

$$F_{2,min} \leq F_2^* \leq F_{2,max} \quad \text{and} \quad F_{3,min} \leq F_3^* \leq F_{3,max} \quad (12)$$

These constraints ensure that the optimized outputs F_2^* and F_3^* remain within the allowable range of the gimbal. It should be noted that minimizing the quadratic cost function alone does not guarantee formal closed-loop stability for nonlinear constrained MPC. In the present architecture, practical closed-loop stability is achieved through the combination of bounded control inputs, actuator limits, and the high-rate inner-loop angular-velocity damping, which provides sufficient phase margin and disturbance rejection under experimental conditions.

E. Angular velocity P controller

With the MPC controller design complete, the next step is to design angular velocity P controllers for both pitch and roll channels that run on the flight controller in Fig. 15, in order to overcome the computational latency of the MPC on the ground-based computer and the communication and processing delays between the onboard flight controller and the ground system. As noted above, MPC wireless communication and calculation latency present major hurdles for MPC deployment. As before, only the pitch channel is detailed here, while the roll channel is omitted due to its similarity.

To mitigate these issues, we augment the MPC output with an onboard pitch rate feedback P controller, defined as $K_{\dot{\theta}}\dot{\theta}$, which operates directly on the flight controller at 400 Hz with a 2.5 ms interval (Fig. 15). This P controller modifies the gimbal command F_3^* generated by the MPC optimizer running on the ground computer, thereby enabling timely correction of deviations during the latency of MPC communication and computation. The modification is applied exclusively to F_3^* rather than simultaneously to F_2^* , because the latter exerts only a limited effect on the pitch angular velocity of the HFWR MAV. This limitation arises from the negligible moment arm associated with F_2^* , which produces an insufficient pitching moment. In contrast, F_3^* , acting perpendicularly to the HFWR MAV's body, provides a more direct and effective contribution to pitch-axis control. This leads to the modified control input:

$$\widetilde{F}_3^* = F_3^* - K_{\dot{\theta}}\dot{\theta} \quad (13)$$

This local damping augmentation (negative feedback) compensates for high-frequency pitch oscillation that might otherwise exploit the MPC's latency. Consequently, the final commands F_4 and θ for the gimbal are derived from $(F_2^*, \widetilde{F}_3^*)$ rather than (F_2^*, F_3^*) . This blended approach preserves the global optimization benefits of a MPC while enabling rapid local corrections against vibrations. Note that the offboard MPC is not used merely as a static trim generator. Even with a short horizon ($N = 3$), it performs receding-horizon optimization under constraints to continuously update the low-frequency attitude command in response to measured state deviations, actuator limits,

and slowly varying disturbances; this constitutes predictive, constraint-aware attitude regulation. Given the low-frequency role of the MPC and the end-to-end latency constraints, a horizon of $N = 3$ was found, through iterative flight testing, to provide a practical balance between control performance and real-time feasibility. The onboard rate loop complements this by injecting fast damping that the delayed offboard optimizer cannot provide. This two-layer construction enables practical deployment on the 110 g prototype: the offboard MPC provides low-frequency attitude regulation and constraint-aware command updates, while the onboard rate loop ensures high-frequency damping and robustness during the latency interval. This is how the E-MPC pitch controller is constructed on the HFWR MAV, and this method enables its deployment on a physical prototype with a total weight of only 110 grams in the next Section.

V. Experimental Validation of E-MPC Effectiveness via Flight Testing

A. Experimental setup

To assess the effectiveness of the E-MPC strategy in stabilizing the HFWR MAV, a series of controlled flight tests was performed. The primary objective was to compare the attitude stabilization performance of three control approaches under pronounced structural oscillations during flight: a) C-PID Controller; b) S-MPC (the MPC shown in Fig. 15 without the additional pitch and roll rate feedback P controllers), and c) E-MPC. The experiments were conducted using the BioMAV-Sim platform developed by the authors, shown in Fig. 20 [13].

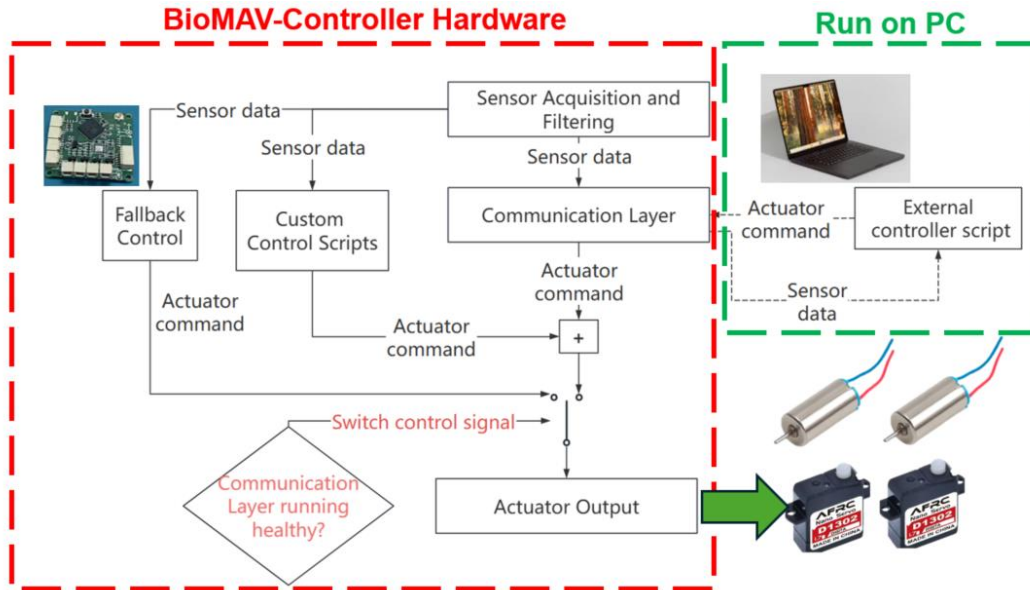


Fig. 20 BioMAV-Sim platform HIL deployment framework

BioMAV-Sim is an in-house hardware-in-the-loop (HIL) testing platform developed specifically for bio-inspired MAV flight control research. The system integrates a computational layer running on a ground-based PC with a flight control layer implemented on the HFWR MAV's embedded controller. It also incorporates a custom-designed HIL flight controller weighing only 3.4 grams, allowing seamless real-time deployment and evaluation of control algorithms. The platform is particularly suited for implementing the E-MPC framework, in which the MPC component executes on an external computer while the P controller operates onboard the flight controller.

In the current study, the MPC function is executed as a Python script on the external offboard component. It receives sensor data from the BioMAV-controller hardware via a high-speed wireless UDP network and outputs actuator commands - specifically, the $(F_2^*, \widetilde{F}_3^*)$ which are then transmitted back to the BioMAV controller. Meanwhile, the custom-developed control script executes the onboard P controller designed in Fig. 15. This enhances real-time responsiveness and performs output blending with the MPC component according to Eq. (13). The blended thrust vectoring actuator commands are sent via the actuator output module in Fig. 20 to the two servos mounted on the HFWR MAV's thrust vectoring gimbal, thereby realizing the closed-loop flight control.

This architecture defines the physical implementation of the E-MPC on the HFWR MAV. For comparison, the deployments of the C-PID and S-MPC controllers are relatively straightforward. The C-PID controller is implemented entirely within the custom control script on the onboard BioMAV controller, without invoking any external offboard control processes. In contrast, the S-MPC configuration executes the MPC solely within the external offboard control script, excluding both the onboard proportional controller and the blending mechanism described in Eq. (13).

Following deployment, all experiments were conducted in actual flight conditions, focusing on two primary tasks:

1. Maintaining a constant pitch attitude reference (target pitch angle = 0°) to evaluate each controller's capability to stabilize the HFWR MAV under pronounced structural oscillations.
2. During Task 1, both reference signals and sensor-derived pitch angle were recorded to compare the control performance of the three controllers: C-PID, S-MPC, and E-MPC, using key quantitative metrics, including peak deviation, overshoot, steady-state error, recovery time, and total energy consumption.

B. Flight test

Following the experimental setup outlined above, physical flight tests were performed on the HFWR MAV using the three control strategies described above. During each test, the rotor flapped at a frequency of 5 Hz, with its

rotational speed gradually increased until the vehicle achieved lift-off and attempted to enter a hover. Throughout the experiments, the desired pitch, roll, and yaw angles were maintained at zero, allowing direct assessment of each controller's capability to sustain attitude stability. The gyroscope's pitch-axis measurements were recorded to quantify the fluctuations around the reference signal under each control approach.

After multiple flight trials, it was observed that both the C-PID and S-MPC controllers failed shortly after takeoff. Specifically, the C-PID controller maintained stable flight for 4.52 s before crashing, while the S-MPC sustained flight for only 2.37 s. In contrast, the E-MPC achieved stable flight for 21 s and 16 s in two consecutive trials, respectively, followed by controlled landings, clearly demonstrating its superior control authority and robustness in stabilizing the HFWR MAV.

Because both the C-PID and S-MPC controllers failed within approximately five seconds of takeoff, with the S-MPC remaining airborne for only about two seconds, a consistent two-second flight window was selected for performance comparison across all controllers in Fig. 21. Fig. 21(a) shows the pitch-angle response within this two-second period for each controller, based on pitch-axis data recorded by the BioMAV controller's onboard sensors. Fig. 21(b) presents the pitch-angle response of the E-MPC during its successful 16-s hover flight.

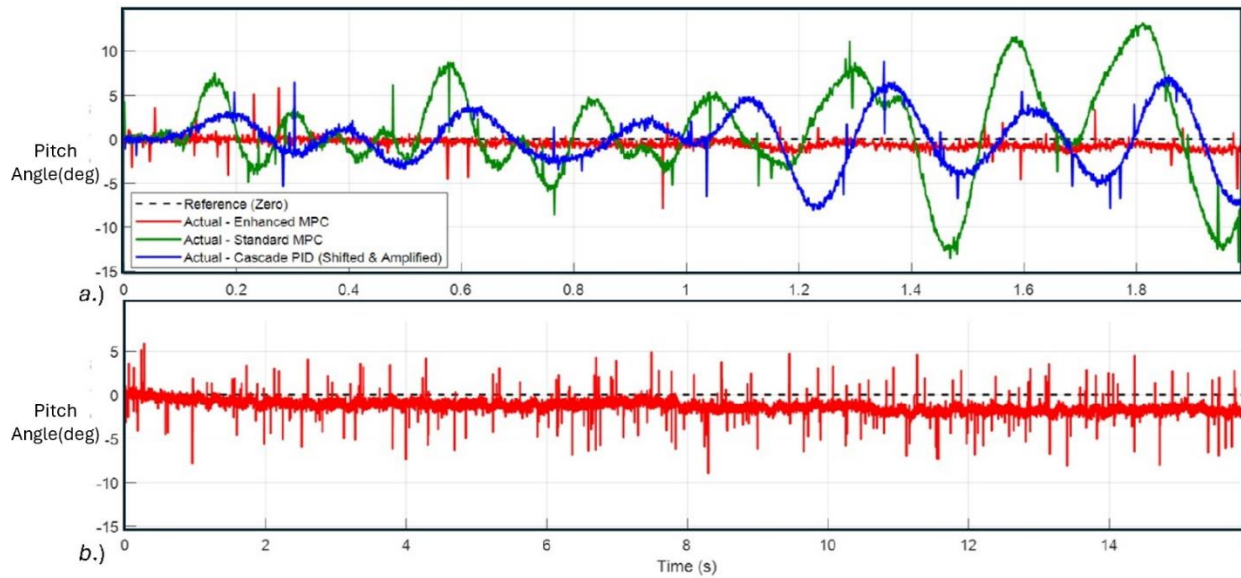


Fig. 21 Comparison of pitch angle stability performance of the three controllers in the flight test

The E-MPC results in Fig. 21(a) outperformed the others across all key performance metrics. It maintained pitch attitude deviations within $\pm 2^\circ$ for over 90% of the duration (as illustrated in Fig. 21(b)), with only minor transient spikes below 5° , demonstrating excellent tracking accuracy and noise suppression. In contrast, the S-MPC exhibited

severe oscillations, with deviations exceeding $\pm 50^\circ$ and an increasing amplitude beyond 1.0 s, indicating poor damping. This degradation can be attributed to computational and communication delays, as both E-MPC and S-MPC share the same cost function and weighting configuration. The latency causes the S-MPC output to act on outdated states, effectively creating a positive feedback loop that amplifies system error. The C-PID controller showed moderate performance, with oscillations in the $\pm 30^\circ$ range and a steady-state error within $\pm 10^\circ$, though a gradual increase in amplitude indicated insufficient damping or suboptimal gain tuning. The above comparison also helps to isolate the roles of the two layers. For completeness, it should be noted that the S-MPC baseline was implemented under the same practical constraints and tuned using the same iterative flight-test-driven procedure as the proposed controller, ensuring that the observed performance differences are not due to insufficient baseline tuning. The poor performance of S-MPC indicates that, under the measured end-to-end delay (>25 ms), the offboard optimizer alone cannot provide sufficient damping against high-frequency angular-rate excursions, and the resulting phase lag can effectively amplify oscillations. In contrast, adding the high-rate onboard rate loop in E-MPC restores the required damping margin and enables the MPC to focus on its intended function, namely low-frequency attitude regulation and constraint-aware command generation. Therefore, the stabilization observed in E-MPC should be interpreted as a consequence of the integrated architecture, in which the onboard loop provides rate damping while the MPC governs the attitude channel and trims the control inputs under constraints. Additional ablation studies, such as a P-only baseline, systematic rate-gain variation, or deliberately reduced rate-loop authority, would further quantify the isolated contribution of the MPC layer. Such additional experiments were not feasible within the current project resources, and they are identified as an important direction for future work.

The grouped bar chart in Fig. 22 presents a comparative evaluation of the three controllers across five critical metrics: peak deviation, overshoot, steady-state error, recovery time, and normalized total energy consumption. The definitions of these metrics are as follows. The peak deviation is defined as the maximum absolute difference between the system output $\theta(t)$ and the reference trajectory $\theta_{ref}(t)$ over the evaluation period $[0, T]$, and is given by:

$$Peak\ deviation = \max_{t \in [0, T]} |\theta(t) - \theta_{ref}(t)| \quad (14)$$

The overshoot quantifies the extent to which the output exceeds the final reference value θ_{ref}^{final} and is expressed as Eq. (15). For this evaluated segment, the reference value is fixed at $\theta_{ref}^{final} = 0^\circ$. The overshoot quantifies the extent to which the output exceeds the reference during the initial transient phase of the response. To avoid redundancy with

the peak deviation metric, which captures the maximum absolute tracking error over the entire evaluation period, overshoot is evaluated only within a bounded time interval $t \in [0, t_s]$, where t_s marks the end of the transient response. Formally, it is defined as,

$$\text{Overshoot} = \max(\theta(t) - \theta_{ref}(t)) \quad , t \in [0, t_s] \quad (15)$$

The parameter t_s is selected to capture the initial rising segment of the response without including later oscillations or steady-state fluctuations. In this study, t_s is determined either by the first time the output enters and remains within a predefined steady-state tolerance band ($\pm 5\%$), or by a fixed duration of 0.5 seconds, whichever is shorter. This ensures that the overshoot metric focuses on the true transient behaviour immediately following a control action.

The steady-state error is calculated as the average absolute tracking error over the final portion of the evaluation window, typically the last T_s seconds, and is given by,

$$\text{Steady state error} = \frac{1}{T_s} \int_{T-T_s}^T |\theta(t) - \theta_{ref}(t)| dt \quad (16)$$

The recovery time is defined as the earliest time $t_r \in [0, T]$ such that the tracking error remains within a specified tolerance ε (in this case, $\varepsilon = \pm 5^\circ$) for the rest of the interval, formally written as:

$$\text{Recovery time} = \min\{t_r \in [0, T] \mid |\theta(t) - \theta_{ref}(t)| < \varepsilon, \forall t \in [t_r, T]\} \quad (17)$$

Finally, the normalized total energy consumption is computed by integrating the squared control input $u(t)$ over time and normalizing the result by the maximum value observed among all controllers, as,

$$\text{Normalized Energy} = \frac{\int_0^T u^2(t) dt}{\max(\int_0^T u^2(t) dt)} \quad (18)$$

It should be noted that this metric does **not** represent the true electrical energy consumption of the HFWR system. The actual electrical power consumption depends on multiple factors, including motor current, voltage, servo internal dynamics, and actuator efficiency, which are not directly measured in the present experiments.

Instead, Eq. (18) is used as a normalized control-effort proxy to quantify the relative intensity of actuator usage during the evaluation period. Integrating the squared control input penalizes excessive actuator activity, including large control amplitudes and high-frequency oscillatory commands. Such behaviors are generally associated with increased actuator workload, mechanical stress, and energy dissipation.

All five critical metrics are computed over the same two-second evaluation window as previously mentioned. This standardized timeframe ensures that, even for the unstable controllers, the results remain meaningful and reflect their performance during their short but operationally relevant period.

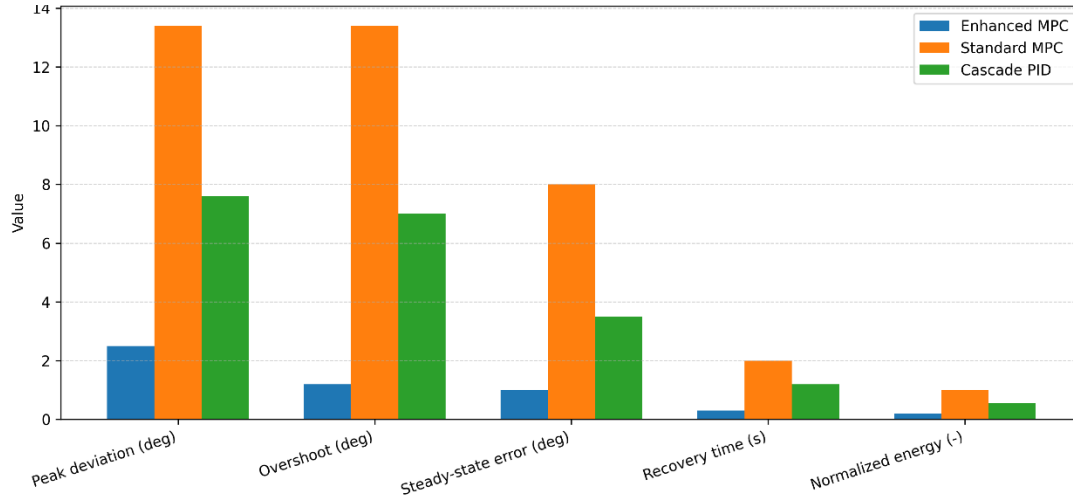


Fig. 22 Performance comparison of controllers across multiple metrics

Fig. 22 shows that the E-MPC outperforms the others in all categories. It achieves the lowest peak pitch attitude deviation at just 5° , significantly outperforming C-PID (30°) and S-MPC (50°). Its overshoot remains within 2° , while C-PID and S-MPC experience overshoots of 25° and 45° , respectively. In terms of steady-state accuracy, the E-MPC maintains pitch deviations within $\pm 1^\circ$, compared to $\pm 5^\circ$ for C-PID and $\pm 30^\circ$ for S-MPC. The E-MPC also achieves the fastest recovery, stabilizing the system within 0.3 s, while both C-PID and S-MPC require over 2 s, with the latter failing to achieve complete convergence. Regarding control effort, the E-MPC shows the lowest normalized energy consumption (1.0), compared to 3.0 for C-PID and 5.0 for S-MPC. These findings establish the E-MPC as the most effective strategy, combining high accuracy, rapid stabilization, and energy efficiency.

Fig. 23 illustrates the flight trajectories during the 21-second and 16-second trials using the E-MPC controller. In both cases, the HFWR maintained stable flight with no significant deviations in pitch or roll, remaining closely aligned with the reference values of zero degrees.

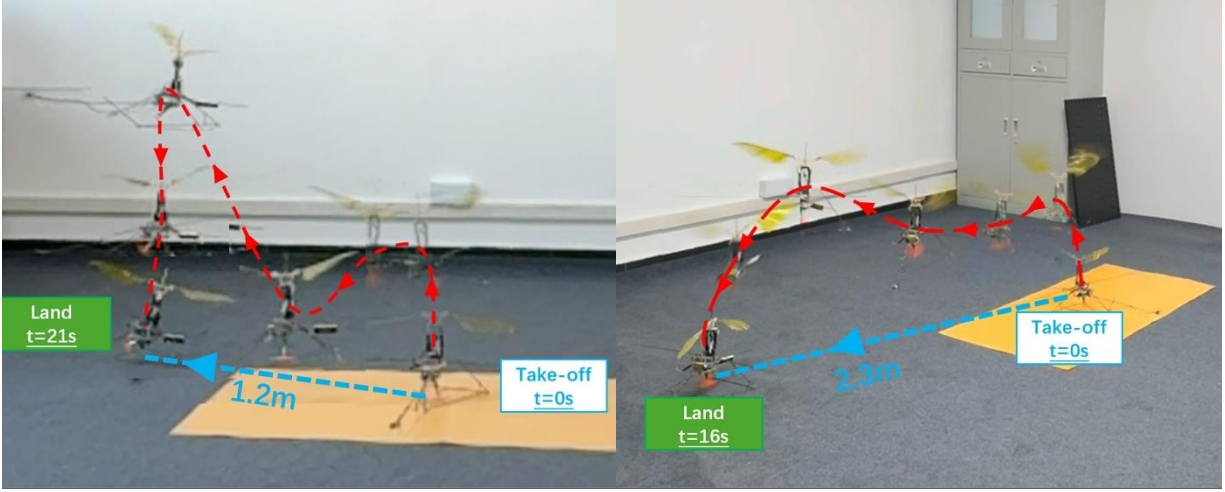


Fig. 23 Flight test with E-MPC hover (left: 21s and right: 16s)

Building upon the configuration, the yaw control experiment retained the pitch and roll reference angles at zero while commanding a desired yaw angular velocity through manual inputs. The results are shown in Fig. 24, the HFWR successfully executed both right and left yaw maneuvers while maintaining pitch and roll stability, further validating the controller's robustness.

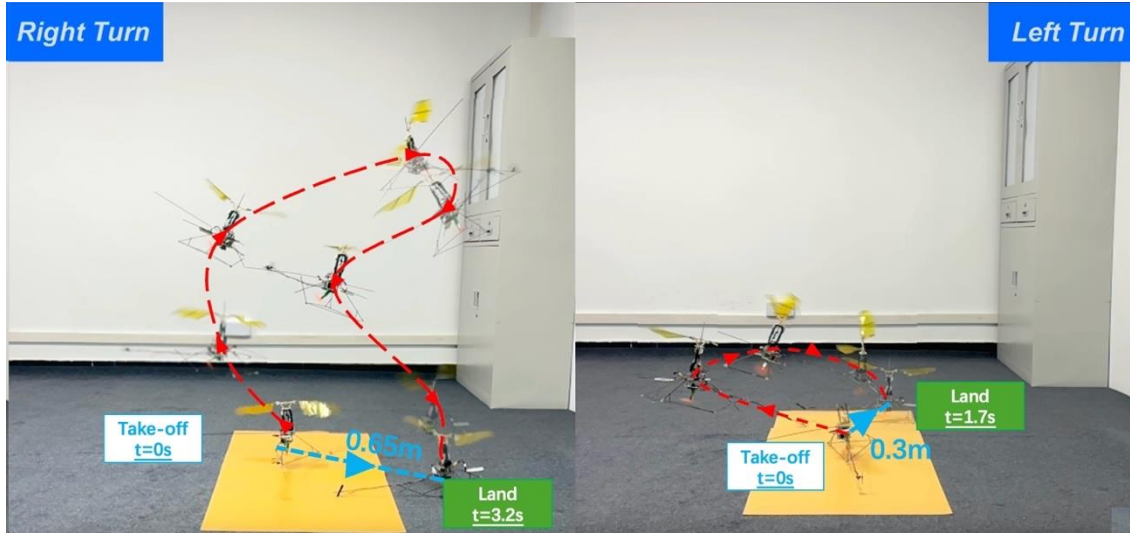


Fig. 24 Illustration of yaw flight test with E-MPC controller

Although a lateral drift between takeoff and landing points, indicated by the blue arrows, is visible, this issue is expected to be resolved in future work by integrating visual cueing feedback, such as optical flow sensors, for closed-loop position control. Nonetheless, the current experiments verify the feasibility and effectiveness of the E-MPC architecture. Videos of the real-world flight tests for all three controllers have been uploaded to YouTube [42]. It should be noted that the current experiments focus primarily on attitude stabilization during hover and yaw

manoeuvres. Commanded aggressive pitch or roll manoeuvres, external disturbance injections, and closed-loop position control were not included in the present study. As a result, the term “controllable flight” in this work refers specifically to stable attitude regulation and sustained hover, rather than full trajectory tracking or position-hold capability. The observed lateral drift is an expected consequence of the absence of closed-loop position feedback and will be addressed in future work using additional sensing modalities such as optical flow or visual odometry.

Fig. 25 shows a 5-s segment (437–442 s) of flight data from a separate test, downsampled to remove excessively dense data points for clarity. The throttle trace represents the pilot’s remote command for motor power, which was increased to 65 % to initiate takeoff. During the subsequent flight period (438–442 s), both the pitch and roll axes remained highly stable, with maximum deviations within $\pm 10^\circ$. These results demonstrate the capability of the E-MPC controller to maintain attitude stability under strong oscillatory disturbances induced by flapping-wing motion. At 442.2 s, the throttle was reduced to zero to initiate landing, producing the transient impact response observed in both the pitch and roll axes near 442.5 s.

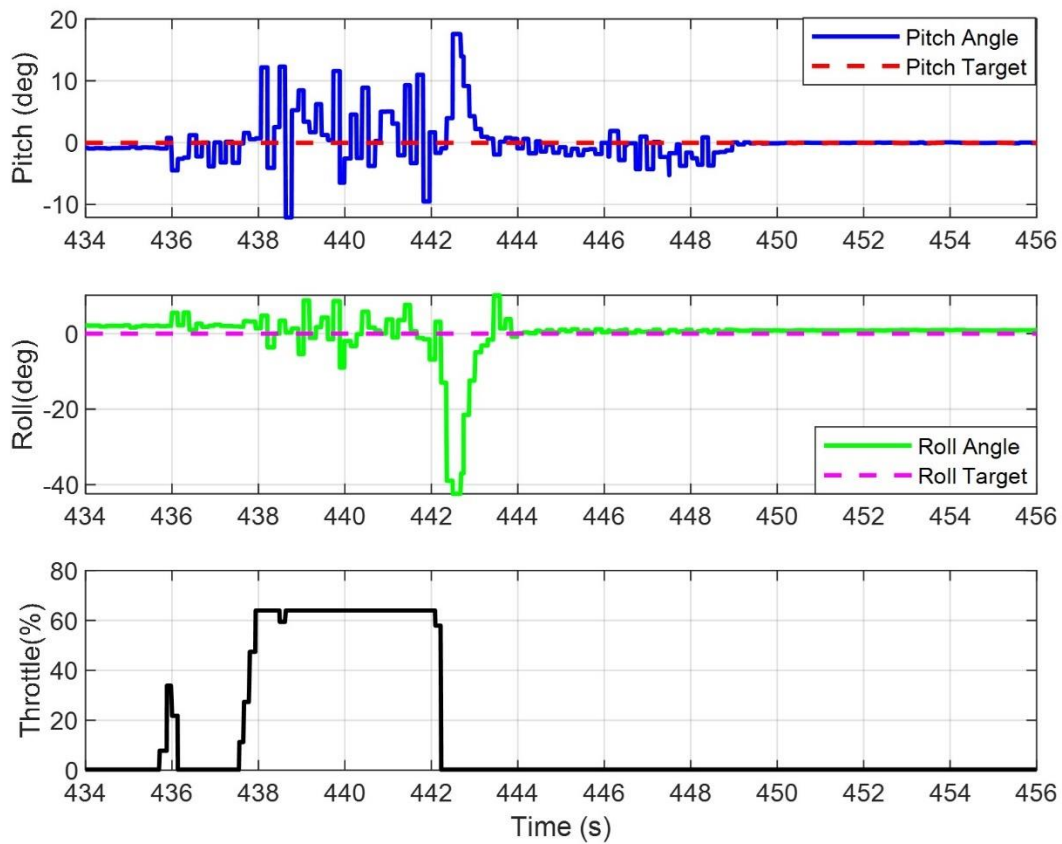


Fig. 25 Flight data demonstrating pitch, roll, and throttle responses during take-off, stable hovering, and landing.

VI. Conclusions

This study demonstrated stable and controllable flight of a Hybrid Flapping-Wing Rotor (HFWR) micro aerial vehicle under strong flapping-induced oscillatory disturbances by combining a thrust-vectoring gimbal airframe and a distributed two-layer control architecture (E-MPC). The key contribution is the integrated implementation: an offboard MPC layer that provides constraint-aware low-frequency attitude regulation and trim/bias compensation, together with an onboard high-rate angular-rate loop that supplies fast damping against high-frequency oscillations and latency effects.

- The E-MPC integrates an offboard MPC optimizer with an onboard proportional rate controller, achieving robust performance despite communication and computation delays.
- A reduced-order model retaining the pitch and roll dynamics captured the essential HFWR behavior while ensuring computational tractability for real-time MPC prediction.
- Hover and Yaw flight tests showed that the E-MPC maintained attitude stability under strong flapping-induced oscillations, with peak deviation, overshoot, and steady-state error reduced by up to 83%, 92%, and 80%, respectively.
- The proposed controller achieved an 80% reduction in total energy consumption relative to conventional control methods.

Future work will focus on extending the E-MPC framework to fully coupled six-degree-of-freedom dynamics, enabling integrated position–attitude control and autonomous flight of bio-inspired rotary-wing MAVs. Additional research will address onboard computation for real-time MPC execution and explore adaptive modeling techniques to further enhance robustness under complex aerodynamic interactions.

Furthermore, the present study employed primarily empirical tuning of MPC weights based on iterative flight-test outcomes, as strong disturbances, unmodeled oscillations, actuator limits, and limited onboard computational resources made exhaustive systematic optimization impractical within the current experimental campaign. Future work will therefore include systematic sensitivity analysis and automated or semi-automated tuning methods to improve reproducibility and robustness.

These efforts will be complemented by dedicated ablation experiments, including P-only baseline controllers and systematic variation of rate-loop gains and control authority, in order to quantitatively separate the respective contributions of the MPC and the onboard rate loop.

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