

# The influence of the dynamics of an autonomous anti-aircraft system on the effectiveness of tracking and intercepting an air target

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## ABSTRACT

The increasing deployment of unmanned ground vehicles (UGVs) demands advanced stabilization systems capable of maintaining precise line-of-sight (LOS) target tracking under dynamic operational conditions. This paper presented a comprehensive parametric simulation framework for the preliminary design and evaluation of turret stabilization algorithms on a moving vehicle. The proposed approach couples a seven-degree-of-freedom (7-DOF) suspension model with three-dimensional kinematic coordinate transformations and a nonlinear two-axis turret model. To mitigate the disturbances arising from terrain irregularities, turning maneuvers, and weapon recoil, a cascade PID control architecture augmented with gravitational feedforward compensation and practical actuator constraints was implemented. Numerical simulations conducted in the MATLAB/Simulink environment demonstrate that the control system successfully compensates for transient disturbances, easily satisfying the  $< 2^\circ$  accuracy requirement for target acquisition. However, maintaining the stringent  $< 0.25^\circ$  precision tracking limit during sustained broadband terrain excitation remains challenging. The developed framework serves as a robust tool for future remotely-operated weapon station design, indicating that structural inertia optimization and improved sensor noise filtering are key pathways for further performance enhancement.

**Keywords:** unmanned ground vehicle, turret control, cascade PID, target tracking.

## INTRODUCTION

The increasing deployment of unmanned and remotely operated combat vehicles has significantly expanded the demand for advanced stabilization and fire-control systems capable of maintaining accurate target tracking under highly dynamic operating conditions. Modern unmanned ground vehicles (UGVs) are expected to conduct reconnaissance, force protection, and fire support missions while traversing rough terrain, negotiating obstacles and performing high-mobility maneuvers. These operational requirements impose stringent constraints on the line-of-sight (LOS) stabilization systems, particularly in the applications involving engagement of airborne targets, where even small angular deviations may substantially

degrade tracking performance and weapon effectiveness. Fundamental work on LOS stabilization [1] and inertially stabilized platforms [2, 3] has established the importance of maintaining angular stability under dynamic base excitation.

Turret stabilization has therefore become one of the key enabling technologies for contemporary weapon systems [4, 5]. In practice, stabilization systems must compensate the disturbances generated by vehicle vibrations, suspension deflections, centrifugal loads during turning maneuvers, and transient recoil forces associated with weapon firing [6, 7]. The problem is further complicated by the strong nonlinear coupling between azimuth and elevation motions, inertia variations resulting from changing gun elevation, gravitational unbalance effects, and the limitations imposed by servo

drives as well as sensors. Although modern military platforms achieve high firing accuracy during on-the-move operation, reproducing such performance in compact remotely-operated turrets remains a challenging control problem. Classical and modern PID-based approaches remain widely used due to their robustness and implementation simplicity [8].

Numerous studies have investigated stabilization methods for optical and weapon platforms. Early analyses demonstrated the importance of inertially stabilized sighting systems and LOS control architectures for maintaining target observability under platform disturbances. Subsequent investigations showed that cascade control structures consisting of nested position and velocity loops offer an attractive compromise between tracking accuracy, robustness and implementation complexity. Despite the growing popularity of advanced control techniques, including sliding mode control, disturbance observers, adaptive algorithms and model predictive control, proportional–integral–derivative (PID) controllers remain widely used in industrial and defense applications because of their relatively low computational burden, ease of tuning and proven operational reliability [8].

The dynamic interaction between vehicle motion and stabilized payloads has attracted increasing research interest over the last decade. The vehicle attitude disturbances generated by suspension oscillations have been shown to significantly influence LOS stabilization accuracy, particularly during high-speed off-road operation [9]. Several authors proposed full-car dynamic models incorporating heave, pitch and roll motions to improve prediction of platform excitations and to facilitate controller development [10, 11]. Feedforward compensation strategies combined with classical feedback loops have also demonstrated improved attenuation of chassis-induced disturbances while maintaining acceptable controller complexity. Recent studies on vehicle dynamics and control provide a solid theoretical basis for such modeling approaches [12, 13].

Another important aspect concerns electro-optical tracking systems. The measurements provided by optical seekers are inevitably contaminated by atmospheric disturbances, image-processing uncertainties and platform-induced vibrations. Appropriate filtering and signal conditioning techniques are therefore required to prevent measurement noise amplification, especially in the cascade PID architectures employing derivative action. Several studies indicated that

relatively simple low-pass filtering approaches may provide satisfactory trade-offs between noise attenuation and phase delay for practical implementation in real-time tracking systems. Disturbance rejection and feedforward compensation strategies have also been successfully applied to electro-optical LOS stabilization systems on moving platforms [14, 15].

Although numerous investigations addressed vehicle dynamics, active suspensions and autonomous navigation, comparatively fewer studies focused on integrated simulation environments coupling suspension dynamics, spatial target-tracking kinematics and nonlinear turret models. Consequently, there remains a need for comprehensive simulation frameworks capable of reproducing the combined effects of terrain irregularities, vehicle turning maneuvers, recoil disturbances and actuator limitations on target-tracking performance. Recent developments in turret modeling confirm the importance of incorporating non-linear dynamics and non-smooth effects in realistic simulations of ground vehicle-mounted weapon systems [16].

The study is continuous of work presented in [17]. This study bridged this gap by developing a parametric model of a remotely-controlled turret mounted on a light to medium weight unmanned ground vehicle. A seven-degree-of-freedom suspension model was combined with three-dimensional coordinate transformations describing LOS kinematics and a non-linear two-axis turret model with elevation-dependent inertia characteristics. The stabilization system employed cascade PID controllers augmented by gravitational feedforward compensation and practical actuator constraints [18]. Numerical simulations conducted in the MATLAB/Simulink environment were used to evaluate tracking performance during representative operational scenarios involving uneven terrain traversal, circular vehicle motion and engagement of a maneuvering airborne target. The proposed framework is intended as a tool supporting preliminary design studies and assessment of stabilization algorithms for future remotely-operated weapon stations.

## **MATHEMATICAL MODEL OF THE SYSTEM**

The general architecture of the modeled system is illustrated in Figure 1. A combat vehicle

equipped with a remotely-controlled turret traverses uneven terrain in the inertial reference frame. The turret rotates relative to the chassis about two independent axes, azimuth and elevation, and is tasked with continuously tracking a moving airborne target. The angular position of the target is measured in the turret-fixed coordinate frame by an electro-optical scanning seeker. The vehicle chassis motions induced by terrain irregularities and weapon recoil act as direct disturbances to the observed line-of-sight angles and must be actively compensated by the stabilization system.

### Suspension 7 DOF model

The dynamics of the combat vehicle chassis were described using a rigid body model supported by four independent suspension stations. The chassis model accounted for vertical displacement  $z_b$ , pitch angle  $\theta_b$ , and roll angle  $\phi_b$ . The state vector of the suspension system was defined as follows:

$$x = [z_b, \theta_b, \phi_b, z_{u,1}, z_{u,2}, z_{u,3}, z_{u,4}, \dot{z}_b, \dot{\theta}_b, \dot{\phi}_b, \dot{z}_{u,1}, \dot{z}_{u,2}, \dot{z}_{u,3}, \dot{z}_{u,4}]^T \quad (1)$$

where:  $z_{u,i}$  – vertical displacements of the four unsprung masses (wheels). The vertical displacements  $z_{c,i}$  and vertical velocities  $\dot{z}_{c,i}$  of the chassis corners corresponding to each wheel location were determined using kinematic transformations for rigid body:

$$z_{c,1} = z_b - l_f \theta_b + 0.5 t_f \phi_b \quad (2)$$

$$z_{c,2} = z_b - l_f \theta_b - 0.5 t_f \phi_b \quad (3)$$

$$z_{c,3} = z_b + l_r \theta_b + 0.5 t_r \phi_b \quad (4)$$

$$z_{c,4} = z_b + l_r \theta_b - 0.5 t_r \phi_b \quad (5)$$

where:  $l_f$  and  $l_r$  – distances from the center of gravity (CG) to the front and rear axles, respectively. The track widths for the front and rear axles were given by  $t_f$  and  $t_r$ . The suspension force  $F_{s,i}$  generated at each wheel station was modeled as a combination of linear stiffness  $k_{s,i}$  and damping  $c_{s,i}$  forces:

$$F_{s,i} = -k_{s,i}(z_{c,i} - z_{u,i}) - c_{s,i}(\dot{z}_{c,i} - \dot{z}_{u,i})$$

The vertical accelerations of the unsprung masses  $\ddot{z}_{u,i}$  were driven by the suspension forces and the interaction with the terrain profile  $Z_{t,i}$  via tire stiffness  $k_{t,i}$ :

$$\ddot{z}_{u,i} = \frac{-F_{s,i} - k_{t,i}(z_{u,i} - Z_{t,i})}{m_{u,i}} \quad (6)$$

where:  $m_{u,i}$  – mass of each wheel assembly. The equations governing the motion of the sprung mass (chassis) were derived as follows:

$$\ddot{z}_b = \frac{1}{m_b} \sum_{i=1}^4 F_{s,i} \quad (7)$$

$$\ddot{\theta}_b = \frac{-l_f(F_{s,1} + F_{s,2}) + l_r(F_{s,3} + F_{s,4}) + M_{ps}}{I_y} \quad (8)$$

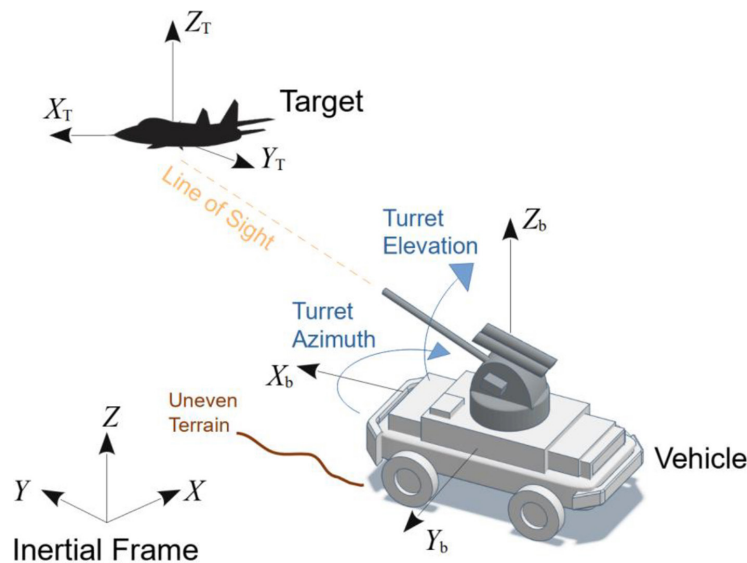


Figure 1. General diagram of the investigated system

$$\ddot{\varphi}_b = \frac{0.5t_f(F_{s,1}-F_{s,2}) + 0.5t_r(F_{s,3}-F_{s,4}) + M_c + M_{rs}}{I_x} \quad (9)$$

In Equation 9,  $M_c$  represents the centrifugal roll moment induced during turning maneuvers, expressed as:

$$M_c = m_b \frac{V_b^2}{R_t} h_{cg} \cos(\varphi_b) \quad (10)$$

where:  $V_b$  – longitudinal velocity,  $R_t$  – turning radius, and  $h_{cg}$  – height of the chassis center of gravity. The terms  $M_{ps}$  and  $M_{rs}$  represent the components of the weapon recoil moment acting on the chassis pitching and rolling planes, respectively:

$$M_{ps} = M_r \cos(\theta_{az}) \quad (11)$$

$$M_{rs} = -M_r \sin(\theta_{az}) \quad (12)$$

where:  $\theta_{az}$  – current azimuth angle of the turret relative to the chassis, and  $M_r$  – recoil moment relative to chassis center of gravity.

### 3D coordinate transformation and tracking kinematics

To maintain the line of sight (LOS) on an airborne target while driving over uneven terrain, a 3D spatial coordinate transformation was required to map the target's position into the vehicle's local coordinate system.

The target trajectory and the vehicle's macroscopic movement were initially defined in an Earth-fixed inertial reference frame. Let  $[X_T, Y_T, Z_T]^T$  represent the absolute position of the target and  $[X_b, Y_b, Z_b]^T$  represent the absolute position of the vehicle's center of gravity (CG). The relative position vector between the target and the moving vehicle,  $[r_x, r_y, r_z]^T = [X_T - X_b, Y_T - Y_b, Z_T - Z_b]^T$ , is continuously computed in a translated inertial frame, i.e. frame the origin of which is attached to the vehicle's CG, but the axes of which remain parallel to the Earth-fixed inertial axes. To express this relative vector in the perturbed vehicle-fixed (chassis) reference frame, a sequential yaw-pitch-roll transformation must be applied. This process systematically reverses the orientation changes introduced by the vehicle's movement and suspension vibrations.

First, the transformation accounts for the vehicle's turning path by compensating for the

yaw angle  $\psi_A$ . This rotated the vector into a heading-aligned frame  $(x_1, y_1, z_1)$ :

$$x_1 = r_x \cos(\psi_A) + r_y \sin(\psi_A) \quad (13)$$

$$y_1 = -r_x \sin(\psi_A) + r_y \cos(\psi_A) \quad (14)$$

$$z_1 = r_z \quad (15)$$

Next, the modifications caused by the chassis pitch angle  $\theta_b$  were countered. This brings the vector into an intermediate pitched frame  $(x_2, y_2, z_2)$ :

$$x_2 = x_1 \cos(\theta_b) - z_1 \sin(\theta_b) \quad (16)$$

$$y_2 = y_1 \quad (17)$$

$$z_2 = x_1 \sin(\theta_b) \quad (18)$$

Finally, the chassis roll angle  $\varphi_b$  transformation was performed to account for lateral tilting. The resulting coordinates  $(x_3, y_3, z_3)$  represent the target's relative position fully expressed within the vehicle-fixed reference frame (attached to the rotating and vibrating chassis):

$$x_3 = x_2 \quad (19)$$

$$y_3 = y_2 \cos(\varphi_b) + z_2 \sin(\varphi_b) \quad (20)$$

$$z_3 = -y_2 \sin(\varphi_b) + z_2 \cos(\varphi_b) \quad (21)$$

The ultimate reference angles for azimuth  $\theta_{az\_ref}$  and elevation  $\varphi_{el\_ref}$  in the moving platform coordinate system were extracted using the four-quadrant inverse tangent function:

$$\theta_{az\_ref} = \text{atan2}(y_3, x_3) \quad (22)$$

$$\varphi_{el\_ref} = \text{atan2}\left(z_3, \sqrt{x_3^2 + y_3^2}\right) \quad (23)$$

### Nonlinear turret dynamics

The plant dynamics describe a coupled, nonlinear 3D turret mechanism. The azimuth moment of inertia  $I_{az}$  varies nonlinearly as a function of the elevation angle  $\varphi$ :

$$I_{az}(\varphi) = I_1 + I_{s0} + a\varphi^3 + b\varphi^2 + c\varphi + d \quad (24)$$

where:  $I_1, I_{s0}, a, b, c$  and  $d$  are constant structural parameters [17]. The elevation-dependent change in inertia introduces Coriolis and centrifugal coupling torques, defined by the partial derivative:

$$\frac{\partial I_{az}}{\partial \varphi} = 3a\varphi^2 + 2b\varphi + c \quad (25)$$

The state-space differential equations representing the coupled azimuth and elevation motions are formulated as follows:

$$\ddot{\theta} = \frac{M_1 + F_1 - h_1 \dot{\theta} - \frac{\partial I_{az}}{\partial \varphi} \dot{\varphi}}{I_{az}(\varphi)} + K_1 \quad (26)$$

$$\ddot{\varphi} = \frac{M_2 + F_2 - h_2 \dot{\varphi} - mgr \cos(\varphi + \gamma) + 0.5 \frac{\partial I_{az}}{\partial \varphi} \dot{\theta}^2}{I_2} + K_2 \quad (27)$$

where:  $M_1$ ,  $M_2$  are the actual driving moments delivered by the servomotors,  $h_1$ ,  $h_2$  are viscous friction coefficients,  $F_1$ ,  $F_2$  represent external disturbance torques (including ballistic forces), and  $K_1$ ,  $K_2$  denote the kinematic base disturbances projected onto the turret axes from the vibrating chassis.

The gravitational torque acting on the unbalanced elevation mass is defined by parameters  $m$ ,  $r$  and  $\gamma$ .

## CONTROL SYSTEM DESIGN

### Control law

The primary objective of the control system is to stabilize the turret's LOS and track the reference angular positions ( $\theta_{ref}$  for azimuth and  $\varphi_{ref}$  for elevation) while mitigating disturbances arising from the vehicle's movement over uneven terrain and the gun recoil forces. To achieve high-performance tracking and stabilization, a cascade Proportional-Integral-Derivative (PID)

control architecture was implemented independently for both the azimuth and elevation axes. General block diagram for one control channel is shown in Figure 2.

The cascade control structure utilized dual feedback loops: an outer loop for position control and an inner loop for velocity regulation. In the azimuth axis, the reference angular velocity  $\dot{\theta}_{ref}$  was generated by the outer PID controller acting on the position error (where  $e_{\theta} = \theta_{ref} - \theta$ ). The velocity error  $e_{\dot{\theta}}$  fed into the inner PID controller was then formulated by comparing the actual angular velocity  $\dot{\theta}$  with  $\dot{\theta}_{ref}$ . This is expressed as:

$$e_{\dot{\theta}} = \dot{\theta}_{ref} - \dot{\theta} \quad (28)$$

An analogous cascade control scheme was applied to the elevation axis, where the reference velocity is  $\dot{\varphi}_{ref}$  and the velocity error  $e_{\dot{\varphi}}$  is determined by:

$$e_{\dot{\varphi}} = \dot{\varphi}_{ref} - \dot{\varphi} \quad (29)$$

These velocity errors were then processed by their respective inner (secondary) PID controllers to produce the demanded control signals. Furthermore, a feedforward control path was implemented for the elevation axis to compensate for the static gravitational moment acting on the unbalanced mass of the gun barrel. The feedforward moment  $u_{FF}$  was calculated analytically as a function of the elevation angle  $\varphi$ , which helps offload the integral action of the elevation PID controller.

The demanded control signals generated by the regulators, denoted as  $u_1$  for the azimuth axis and  $u_2$  for the elevation axis, were given by:

$$u_1 = C_{in,az}(e_{\dot{\theta}}) \quad (30)$$

$$u_2 = C_{in,el}(e_{\dot{\varphi}}) \quad (31)$$

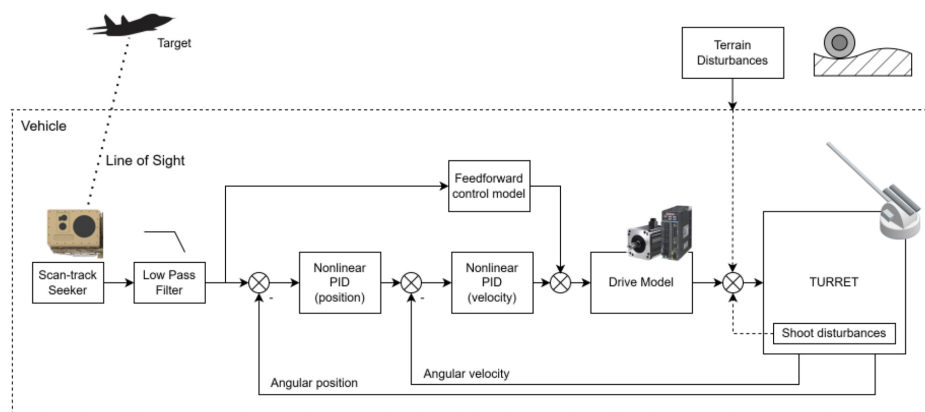


Figure 2. Block diagram of cascade loop for the turret

where:  $C_{in,az}(\cdot)$  and  $C_{in,el}(\cdot)$  denote the control functions of the inner PID controllers for the azimuth and elevation axes,  $u_{FF}(\varphi)$  is feed-forward control for gravity torque compensation calculated from Eq. (28).

In the full numerical simulation, these demanded control signals are further passed through a comprehensive servo drive model to reflect physical constraints. The servo drive model includes a slew rate limiter set to  $\dot{u}_{max} = 6$  Nm/ms, which restricts the maximum permissible rate of change of the demanded torque signal to represent the finite dynamic response of a high-performance servo drive. This value is consistent with the peak torque slew rate achievable by high-end industrial direct-drive servo systems. For reference, Granite Devices IONI-class servo drives specify torque slew rates in the range of 4.8 to 9.5 Nm/ms depending on the motor configuration [18]. A mechanical gear ratio of  $N = 45$  between the motor shaft and the turret axis is also included in the model. Ultimately, the servo drive model generates the actual driving moments  $M_1$  and  $M_2$  that are applied to the turret plant dynamics (Equation 27, 28).

### Line of sight signal filtration

In realistic operational environments, the measurement of the target's LOS angles ( $\theta_{ref}$  and  $\varphi_{ref}$ ) provided by the electro-optical tracking system is inevitably corrupted by measurement noise. This noise arises from atmospheric turbulence, sensor imperfections, and the finite resolution of image tracking algorithms. Based on the literature regarding the optical tracking systems for anti-aircraft applications (e.g., [1, 2]), the measurement noise is typically modeled as zero-mean white Gaussian noise. The angular tracking noise standard deviation  $\sigma$  usually may range from 0.1 to 1.0 mrad (approx.

0.005° to 0.05°) depending on the signal-to-noise ratio, target distance, and platform vibrations.

To prevent this high-frequency noise from propagating through the derivative action of the PID controllers and causing excessive control chattering or saturating the servo drives, a first-order low-pass filter was applied to the raw reference signals. The filter's transfer function was defined as:

$$G_f(s) = \frac{1}{T_f s + 1} \quad (32)$$

where:  $T_f$  is the filter time constant. In the current design, a time constant of  $T_f = 0.05$  s was selected as an compromise between providing adequate noise attenuation and minimizing the phase delay introduced into the tracking control loop.

## SIMULATION ENVIRONMENT

The performance of the proposed control system and the 3D dynamics of the combat vehicle were evaluated through numerical simulations developed in the MATLAB/Simulink environment. The simulation framework integrated the 7-DOF nonlinear vehicle suspension model, the spatial kinematic transformation for LOS tracking, and the highly coupled turret dynamics along with the cascade PID control loops.

The Simulink block diagram of the simulation framework, with the main subsystems highlighted, is presented in Figure 3.

### Model parameters

The physical parameters of the vehicle and the turret used in the simulation represent a medium-class unmanned ground vehicle (UGV) with a  $4 \times 4$  off-road chassis, comparable in size and mass to

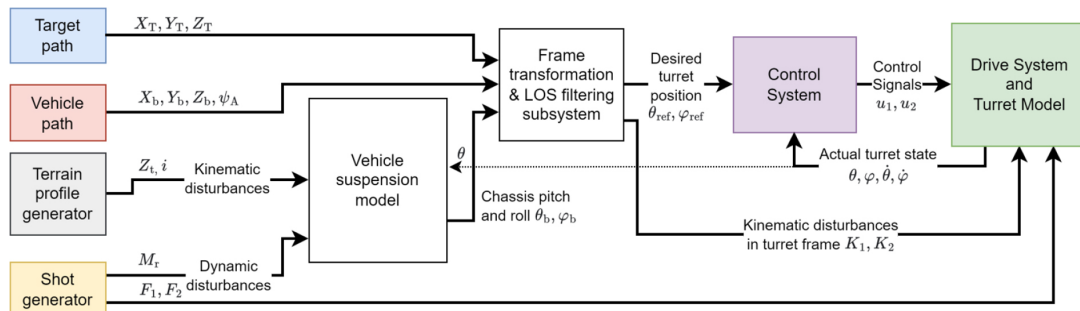


Figure 3. General block diagram of Simulink implementation

modern platforms such as the Rheinmetall Mission Master XT [19]. The nominal sprung mass  $m_b$  of 2400 kg corresponds to a highly mobile, autonomous tactical platform. The moments of inertia ( $I_x$  and  $I_y$ ) were initially estimated using standard rigid-body approximations for a rectangular prism and subsequently scaled to account for the mass distribution of the engine, internal payload and electronics, and the significant top mass of the 420 kg turret system.

It is worth noting that in the 7-DOF suspension model, tire damping was intentionally omitted. In typical off-road and tactical vehicles, the damping coefficient of pneumatic tires is approximately an order of magnitude smaller than the main suspension shock absorbers [10]. Consequently, its influence on the macroscopic heave, pitch, and roll motions of the heavy sprung mass is negligible, justifying the use of a simplified point-contact elastic tire model characterized solely by its vertical stiffness  $k_t$ .

The key parameters of the model are summarized in Table 1. It is worth noting that for the discussed simulation, mainly due to its duration, constant masses and moments of inertia

were assumed for both the vehicle and the turret; hence, the changes caused by the consumption of, e.g., fuel or ammunition depletion were neglected. Nevertheless, if necessary, these parameters can be time-varying and dependent on the operations being conducted.

## Controller parameters

The cascade PID controller gains were initially tuned using the automated PID Tuner tool available in MATLAB/Simulink, which employs a frequency-domain optimization approach to meet specified bandwidth and phase margin targets. The resulting parameters were subsequently refined through manual iterative adjustment based on closed-loop simulation performance, prioritizing fast target tracking response while avoiding oscillation and constant control signal saturation. Both azimuth and elevation controllers include output saturation limits and an anti-windup mechanism based on the back-calculation scheme with coefficient  $K_b$  [8]. The final tuned parameters are summarized in Table 2.

**Table 1.** Key physical parameters of the vehicle and turret model

| Subsystem  | Parameter                           | Symbol         | Value      | Unit              |
|------------|-------------------------------------|----------------|------------|-------------------|
| Chassis    | Sprung mass                         | $m_b$          | 2400       | kg                |
|            | Roll moment of inertia              | $I_x$          | 1500       | kg m <sup>2</sup> |
|            | Pitch moment of inertia             | $I_y$          | 3500       | kg m <sup>2</sup> |
|            | Distance from CG to front/rear axle | $l_f, l_r$     | 1.40, 1.60 | m                 |
|            | Track width                         | $t_f, t_r$     | 1.85       | m                 |
| Suspension | Front/Rear spring stiffness         | $k_s$          | 45, 50     | kN/m              |
|            | Front/Rear damping coeff.           | $c_s$          | 3.5, 4.0   | kN s/m            |
|            | Wheel assembly mass                 | $m_u$          | 85–90      | kg                |
|            | Tire stiffness                      | $k_t$          | 250        | kN/m              |
| Turret     | Unbalanced elevation mass           | $m$            | 420        | kg                |
|            | Distance to elevation mass CG       | $r$            | 0.32       | m                 |
|            | Azimuth base moment of inertia      | $I_1 + I_{s0}$ | 285        | kg m <sup>2</sup> |
|            | Elevation moment of inertia         | $I_2$          | 90         | kg m <sup>2</sup> |
|            | Gravitational unbalance angle       | $\gamma$       | 7          | deg               |

**Table 2.** Tuned nonlinear PID controller parameters

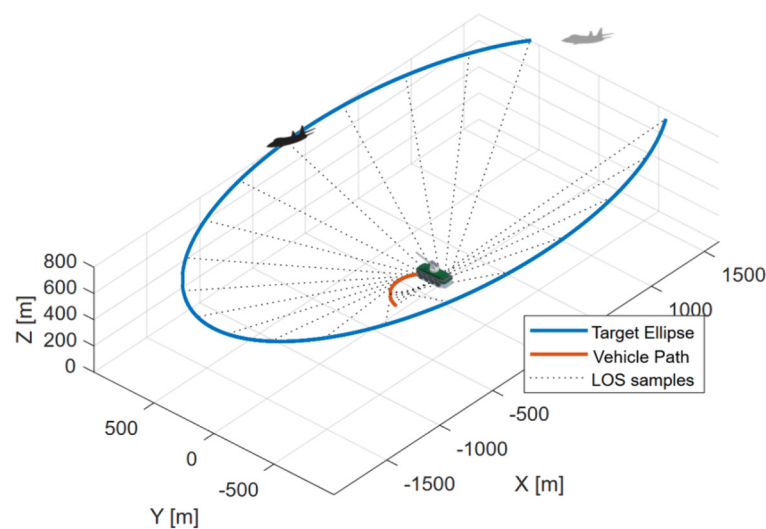
| Axis      | Loop         | $K_p$ | $K_i$ | $K_d$ | Saturation | $K_b$ |
|-----------|--------------|-------|-------|-------|------------|-------|
| Azimuth   | Outer (pos.) | 97.5  | 39.1  | 6.38  | ±2.5 rad/s | 0.67  |
|           | Inner (vel.) | 10.6  | 0     | 8.0   | ±40 Nm     | 0.39  |
| Elevation | Outer (pos.) | 79.9  | 49.5  | 9.44  | ±2.5 rad/s | 1.34  |
|           | Inner (vel.) | 68.8  | 20.1  | 18.8  | ±40 Nm     | 0.50  |

## Target and vehicle trajectory

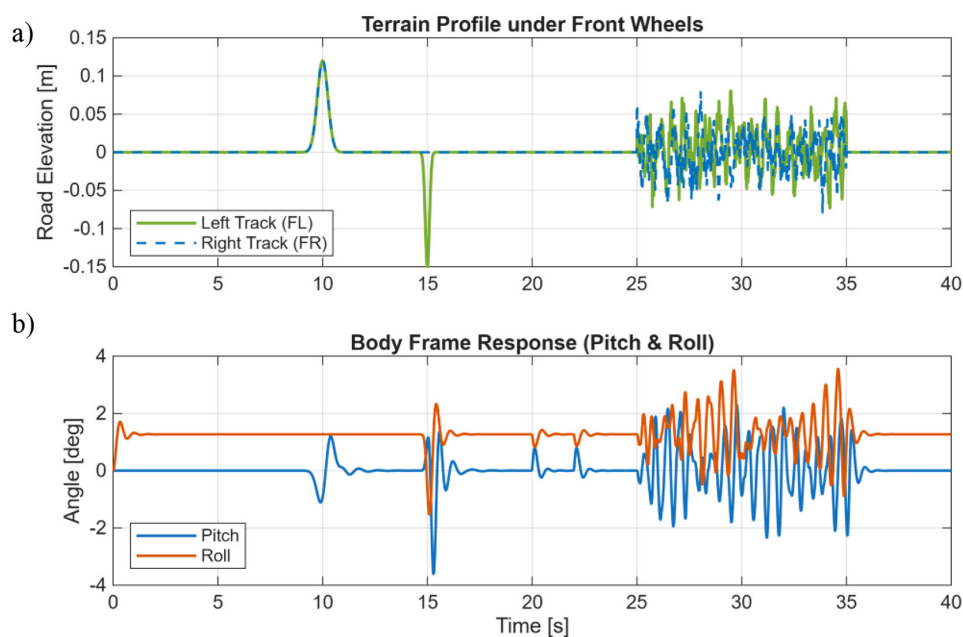
The target was modeled as a point mass following an elliptical trajectory at a constant altitude in the inertial frame. The semi-major and semi-minor axes of the trajectory were  $A_T = 1000$  m and  $B_T = 800$  m, respectively, the flight altitude was  $h_T = 800$  m, and the target speed was  $V_T = 250$  m/s, which is representative of a low-observable unmanned aerial vehicle or a cruise missile. The vehicle simultaneously executed a steady circular turn at a longitudinal speed of  $V_b = 16$  m/s with a turning radius of  $R_t = 220$  m. The resulting relative geometry between the vehicle and the target is illustrated in Figure 4.

## RESULTS

The prescribed terrain profile traversed by the vehicle is shown in Figure 5a. A single convex bump with a peak height of 0.12 m was encountered at  $t = 10$  s, while at  $t = 15$  s the left wheels passed through a pothole with a depth of 0.15 m, introducing a transient pitch and roll disturbance. Between  $t = 25$  s and  $t = 35$  s, the vehicle traversed a randomly uneven surface with an amplitude in the range of  $\pm 0.1$  m, representing a sustained broadband excitation of the suspension. The structural characteristics and amplitude of this random section correspond to an ISO



**Figure 4.** Relative geometry of the target elliptical trajectory and the vehicle circular path in the inertial frame



**Figure 5.** (a) Prescribed terrain profile and (b) resulting body chassis pitch and roll responses

8608 Class D road profile, which is representative of poor surface conditions, such as rough gravel roads or unpaved dirt trails. The resulting chassis pitch and roll disturbance responses are presented in Figure 5b. Additionally, a short firing burst was included at  $t = 20$  s and  $t = 22$  s. The steady-state roll angle observed in the results is due to the centrifugal forces during the UGV turning maneuver.

The reference LOS angles computed from the 3D kinematic transformation and the actual turret angles realized by the cascade PID controllers are shown in Figure 6a for the azimuth axis and Figure 6b for the elevation axis. A transient settling

phase is visible in the initial period of the simulation, during which the turret steers from its rest position onto the reference trajectory.

The azimuth and elevation tracking errors are shown in Figure 7. The stabilization requirements for typical missile engagement were defined as  $< 2^\circ$  for target acquisition and  $< 25^\circ$  for precision fire support. The results indicate that the control system successfully compensates for transient disturbances, although maintaining the precision tracking requirement of  $0.25^\circ$  during the broadband random terrain excitation (25–35 s) poses a significant challenge and is impossible for the current turret stabilization control system.

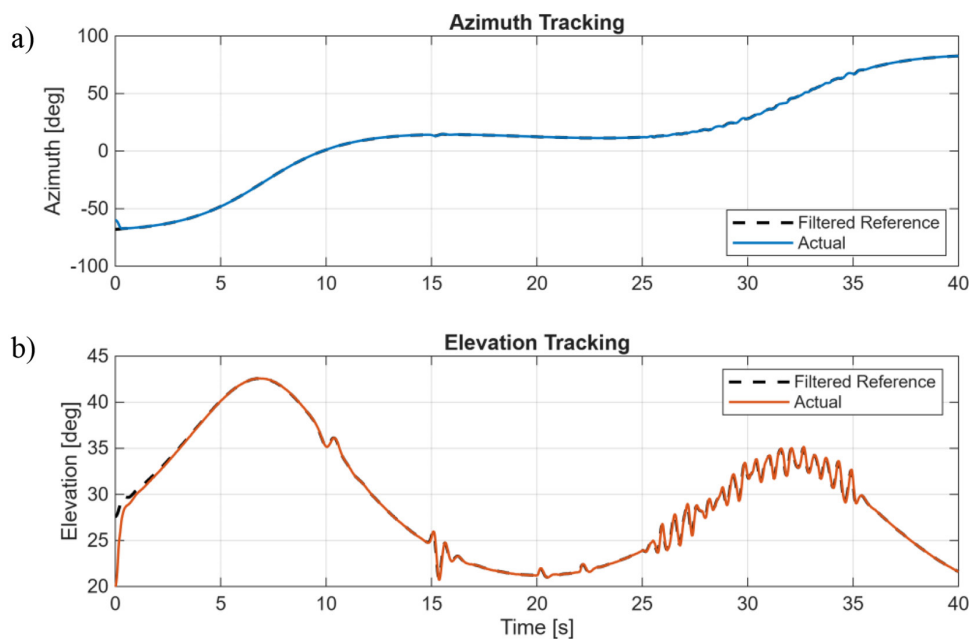


Figure 6. (a) Azimuth and (b) Elevation reference and actual turret angles and over the simulation period

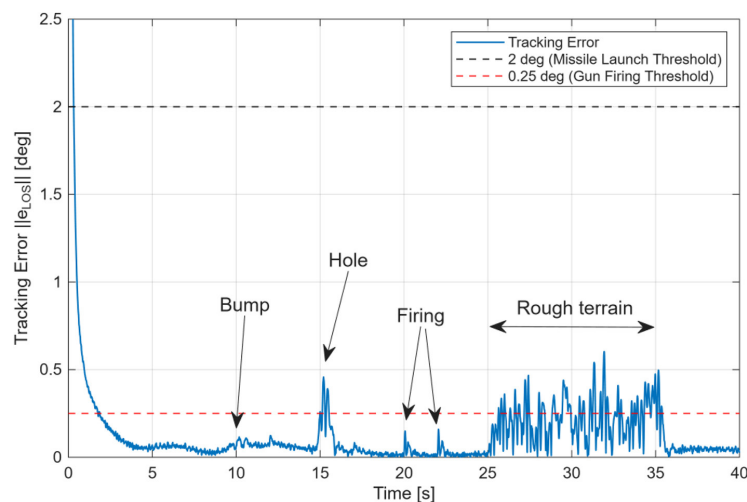
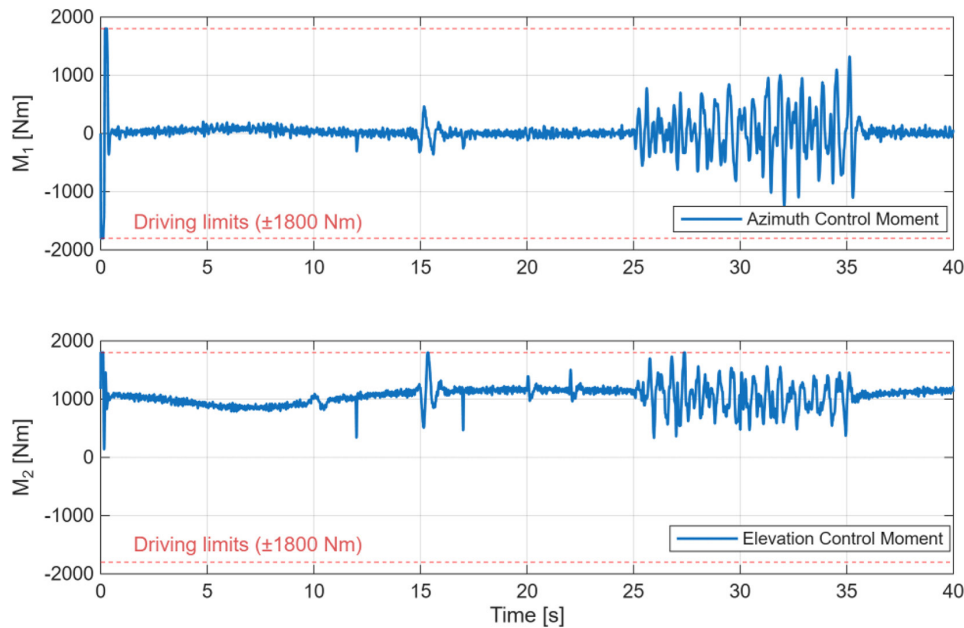


Figure 7. Global tracking error during different disturbances



**Figure 8.** Control moments applied to the azimuth and elevation system of the turret

**Table 3.** Summary of tracking error for selected time windows

| Window   | Start [s] | Stop [s] | Mean $ e_{LOS} $ [deg] | Max $ e_{LOS} $ [deg] |
|----------|-----------|----------|------------------------|-----------------------|
| Bump     | 9         | 11       | 0.066782               | 0.11653               |
| Hole     | 14        | 16       | 0.14555                | 0.45834               |
| Firing 1 | 20        | 21       | 0.038787               | 0.14996               |
| Firing 2 | 22        | 23       | 0.037057               | 0.16014               |
| Rough    | 25        | 35       | 0.21676                | 0.6038                |

The control torques applied to the turret (after mechanical gear) are presented in Figure 8, reflecting both the high-frequency content of left-over of the filtered noise and the active corrective action of the controllers.

Table 3 presents mean and maximum values for tracking error during disturbances acting on the vehicle. It clearly shows that a more advanced control system is required to maintain turret stabilization during assumed rough terrain vehicle movement.

## CONCLUSIONS

This paper presented a comprehensive parametric simulation framework for preliminary design and testing of stabilization algorithms for a turret mounted on a moving ground vehicle. A 7-DOF nonlinear vehicle suspension model was coupled with a 3D kinematic coordinate transformation and a nonlinear turret dynamics

model to capture the disturbances acting on the line of sight. A cascade PID control architecture, augmented with gravitational feedforward compensation, was shown to effectively stabilize and track an airborne target under combined terrain, turning, and gun-recoil disturbances.

The simulation results highlighted the critical importance of high-performance servo drives in achieving the required stabilization accuracy. Sensitivity studies revealed that reducing the drive torque slew rate below approximately 1 Nm/ms leads to instability of the closed-loop system, establishing a minimum dynamic requirement for the actuator specification.

Several directions for further improvement were identified. First, the servo drives operated well within their torque saturation limits throughout the tested scenarios, indicating available headroom that could be exploited by employing more aggressive PID settings. However, realizing this potential requires improved noise rejection in the tracking sensor feedback

path, so that higher derivative gains do not amplify measurement noise into the control signal. Second, a reduction of the turret's effective moment of inertia – achievable, for example, by rearranging the elevation rotation axis relative to the barrel assembly, would reduce the demanded torques and relax the servo-drive requirements, thereby creating additional margin for control performance. These aspects constitute natural extensions of the present work and will be addressed in future research.

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